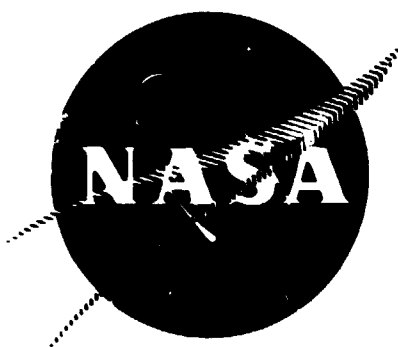


R-8832



DESIGN OF INDUCERS FOR TWO-PHASE OPERATION  
FINAL REPORT

by  
J. A. King

ROCKETDYNE  
A DIVISION OF NORTH AMERICAN ROCKWELL CORPORATION

prepared for

NATIONAL AERONAUTICS AND SPACE ADMINISTRATION  
George C. Marshall Space Flight Center  
Huntsville, Alabama 35812

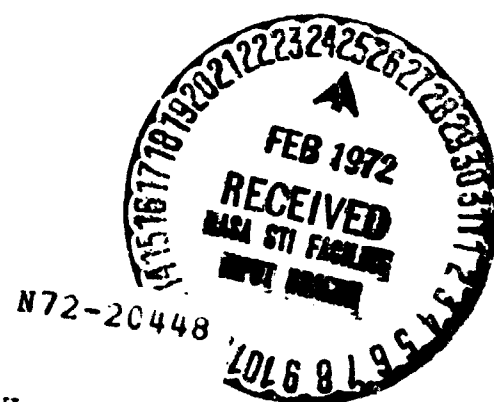
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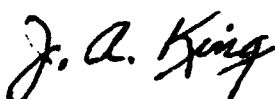
DESIGN OF INDUCERS FOR  
TWO-PHASE OXYGEN  
FINAL REPORT

JANUARY 1972

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George C. Marshall Space Flight Center  
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#### FOREWORD

Rocketdyne, a division of North American Rockwell Corporation, has prepared this final report which documents the work performed in fulfillment of the contract, "Design, Fabrication, Testing, and Delivery of an Oxygen Inducer for Two-Phase Operation," during the period from 12 January 1971 to 12 January 1972. This program was sponsored by the National Aeronautics and Space Administration, Marshall Space Flight Center, Huntsville, Alabama, under Contract NAS8-26645. The work was performed by the Turbomachinery Design and Advanced Turbomachinery Units of the Turbomachinery and Advanced Systems Departments. Mr. Charles Miller, NASA-MSFC, Huntsville, Alabama, was the Technical Project Manager for the program.

#### ABSTRACT

A mathematical model and a digital computer program were developed for the design and evaluation of inducers for pumping two-phase oxygen. The mathematical model indicated that any design would have to be run at relatively low speeds to pump two-phase oxygen and that the Rocketdyne J-2S oxygen inducer was close to optimum and should perform the task. A J-2S pump was built up and tested in two-phase oxygen at speeds indicated by the mathematical model. The pump was able to handle two-phase oxygen over a wide range of inducer incidence angles and up to a vapor content of 37 percent by volume with very little loss in head. The pump was still able to produce some head at vapor fractions approaching 60 percent.

#### ACKNOWLEDGMENT

Acknowledgment is made to Mr. W. R. Bissell for the development of the mathematical model and its correlation with the experimental data, to Mr. G. A. Umemoto for conducting the two-phase oxygen pump testing, and to Mr. F. R. Fishburn who acted as liaison with SSFL and also conducted a portion of the tests.

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## INTRODUCTION

In future space vehicle systems, such as the Space Shuttle Auxiliary Propulsion System (APS) and others, there may be performance requirements with unpressurized tanks, multiple starts in zero-gravity environment and operation at close to saturated propellant conditions. Thus, a need exists for the development of pumps that can operate under these conditions.

In the program conducted for Marshall Space Flight Center under Contract NAS8-25069 (Ref. 1), a mathematical model satisfactory for both design and evaluation of inducers for pumping two-phase hydrogen was developed and programmed for a computer. An inducer was designed, fabricated, and successfully tested in two-phase hydrogen. However, a need still existed for the development of technology for two-phase oxygen pump inducers so that an  $O_2/H_2$  pumping system for space could be achieved that would be free of pressurization systems, preconditioning of propellants, and complex operational constraints and sequencing.

It was the objective of this program to establish design criteria for two-phase oxygen inducers and to develop a mathematical model suitable for both design and evaluation of two-phase oxygen inducers. An inducer-pump combination capable, according to the criteria, of pumping two-phase oxygen was built up and tested in oxygen over a range of qualities, temperatures, speeds, and flowrates. The resultant data were analyzed and compared to the predicted results.

## SUMMARY AND CONCLUSIONS

1. Thirty test runs yielding useful data were conducted in two-phase oxygen, using the Mark 29 oxygen pump. Because this is a centrifugal pump, the range of test liquid flow coefficients was the broadest of any two phase test series conducted by Rockwell to date, the minimum liquid flow coefficient being 32 percent of the maximum. Twenty-two of these runs reached the vapor fraction required for zero NPSH, thereby proving that zero NPSH operation is possible in oxygen if the flowrate is low enough to avoid choking in the inlet line and the pump speed is low enough to keep the liquid incidence-to-blade angle ratio  $(i/\beta)_L$  within the proper range.
2. To obtain predictable operation, two-phase inducers should be designed at liquid incidence-to-blade angle ratios  $(i/\beta)_L$  less than 0.7. Within this limitation, the peak test vapor capacity reached at a pump head loss of 20 percent was approximately 45 percent.
3. If the inlet line is unchoked and if  $(i/\beta)_L$  is less than 0.7, the simple equation below may be used to estimate vapor pumping capacity.

$$\alpha_{\max} = 1 - \frac{\phi_L}{\phi_{L-\text{des}}} \left[ \frac{1 - (i/\beta)_{L-\text{des}}}{1 - B} \right]$$

4. A mathematical model based on compressible two-phase flow relationships was developed and programmed for a computer. This program is able to treat a wide range of inducer geometries in either hydrogen or oxygen and may be easily converted to other fluids. This program's predictions of both inducer discharge conditions and inducer vapor pumping limits showed good agreement with test results.
5. At tip speeds of 60 and 80 ft/sec, which are low enough to avoid inlet line choking, the test inducer operated at two-phase values of  $i/\beta$  between 0.4 and 0.75 with only a slight loss in head.
6. Increasing the inlet vapor pressure, or temperature, increases the inlet acoustic velocity and, therefore, increases (1) the maximum pump flowrate, (2) the maximum pump rotational speed, and (3) for a given rotational speed, the flow range over which vapor can be pumped.

## INDUCER TWO-PHASE FLOW ANALYSIS

### MATHEMATICAL MODEL

The fluid flow phenomena that can occur in the flow of a two-phase fluid are complicated. A rigorous treatment of vaporization and condensation rates in two-phase flow must consider heat transfer rates between the liquid and the vapor bubbles, temperature gradients in both the liquid and the vapor bubbles, surface tension forces, number of nucleation sites, radial inertia gradients, radial pressure gradients, bubble diameters, and slip velocities between the liquid and the bubbles. After all of these interacting phenomena are expressed mathematically, the magnitudes of the bubble diameters and the slip velocities are still unknown and, therefore, must be assumed.

In addition, the number of flow possibilities that may occur as the two-phase fluid passes through an inducer is large. Due to the low acoustic velocity in a two-phase fluid, shock waves are highly probable. If the shock waves are oblique, they may be either strong or weak. Bow waves may occur at the blade leading edges, and normal shock waves may occur in the diverging flow passages. Sudden condensation may occur across a shock wave, and the amount of this condensation may be influenced by the strength of the shock wave. Vapor cavities downstream of flow passage discontinuities are possible. Under certain flow conditions, shock waves may propagate upstream of the blades and turn the flow before it reaches the inducer. Because an inducer has a small hub diameter relative to the tip diameter, the blade velocity and, therefore, the flow conditions are completely different at the hub than at the tip. In addition, choking will occur whenever the flow approaches its acoustic velocity at the inducer throat.

Because some of these flow phenomena are impossible to evaluate and because others do not warrant the effort of a rigorous treatment at this time, it was decided to make enough assumptions to obtain a relatively simple mathematical model with sufficient flexibility to permit the evaluation of first-order

effects. Therefore, certain basic assumptions were made such as homogeneous flow, one-dimensional flow, an equilibrium flow process in the inlet line, and a constant quality flow process within the inducer. The flow patterns were then varied until a reasonable approximation of the test data was obtained. Although it cannot be proved that the resulting mathematical model is a correct representation of what is going on in the inducer, it is reasonable from a theoretical standpoint and has shown satisfactory agreement with all pertinent test data to date.

In the following discussions, the general characteristics of two-phase inducer pumping are summarized first. These general characteristics were developed under a NASA program entitled "Thermodynamic Improvements in Liquid Hydrogen Turbopumps" (Ref. 2) and consist of a method for predicting the vapor pumping limit of an inducer and a method for relating this limit to the vehicle requirements. This summary is followed by a discussion of the two-phase inducer flow model developed under the NASA program entitled "Design of Inducers for Two-Phase Operation" (Ref. 1) and extended to oxygen under the present program.

### General Characteristics

Flow Processes. The two-phase flow is assumed to be a homogeneous mixture of vapor and liquid with zero slip between the two phases. In the inlet duct, the flow has a relatively large residence time at constant flow area and therefore, should be able to approach equilibrium (Ref. 3 and 4). However, relative to moving inducer blades, the residence times are so short and the area changes are so rapid that there is little time for vaporization or condensation to occur and, as a result, the flow process probably does not deviate much from constant-quality flow (Ref. 3). Therefore, equilibrium flow is assumed in the inlet line upstream of the inducer and constant-quality (constant mass ratio) flow is assumed within the inducer flow passages. The effects of these flow processes on acoustic velocity are shown in Fig. 1 for both oxygen and hydrogen.



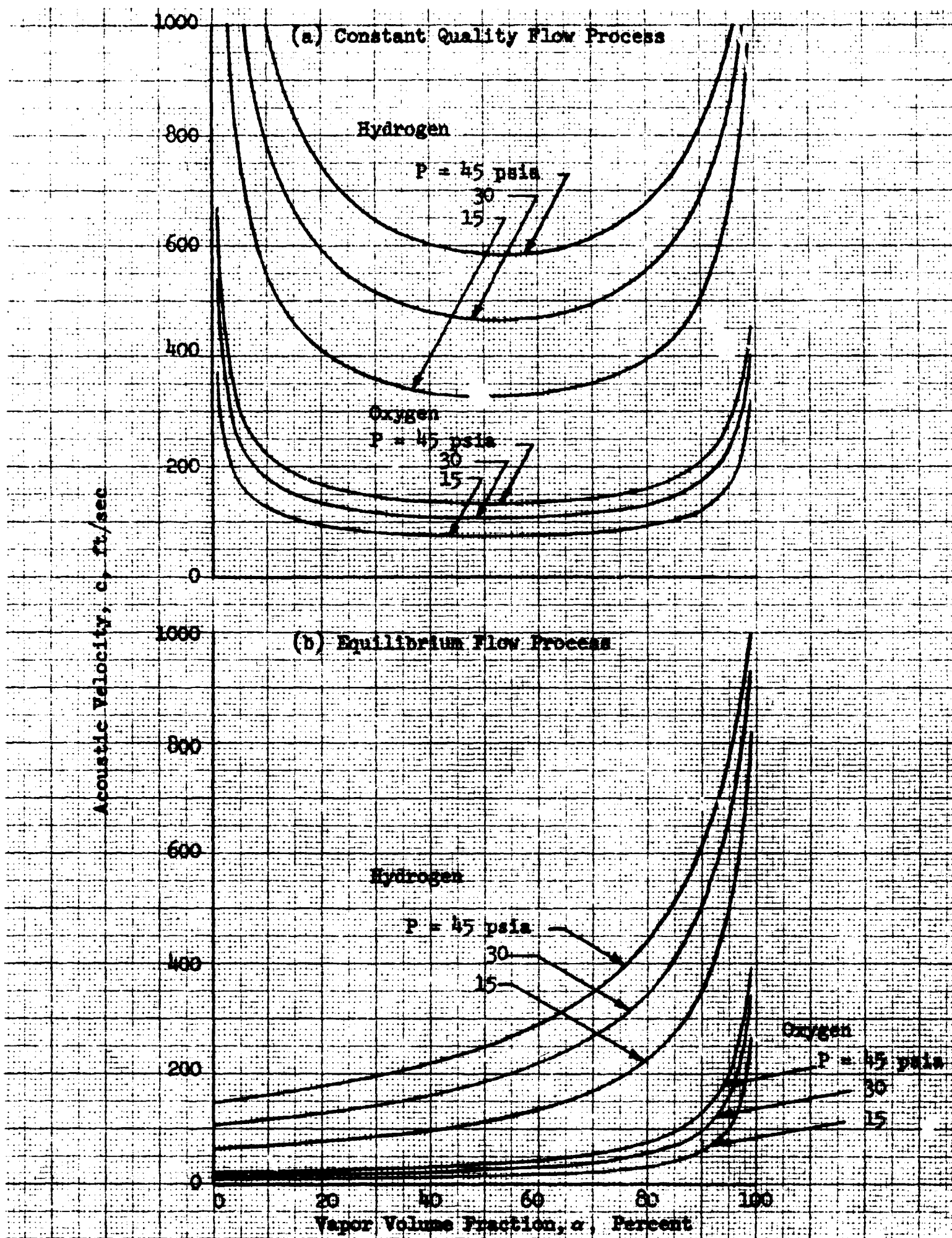


Figure 1. Acoustic Velocities in Two-Phase Oxygen and Two-Phase Hydrogen



Blade Blockage Limit. When the mass flow is constant, an increase in the vapor volume fraction causes the inducer blade flow passages presented to the flow to change from a diverging section (Fig. 2a) to a converging section (Fig. 2b). In addition, the relatively low acoustic velocity of constant-quality two-phase flow (Fig. 1) causes the flow to approach the inducer blades at a Mach number between 0.5 and 2.0. As shown in Fig. 3 a very small area convergence at these high Mach numbers will cause choking and, therefore, will prevent any further increase in vapor volume fraction without a decrease in the liquid volume flow-rate. Due to the small allowable convergence, this choking limit may be approximated by the flow conditions under which the downstream throat area  $A_2$  is equal

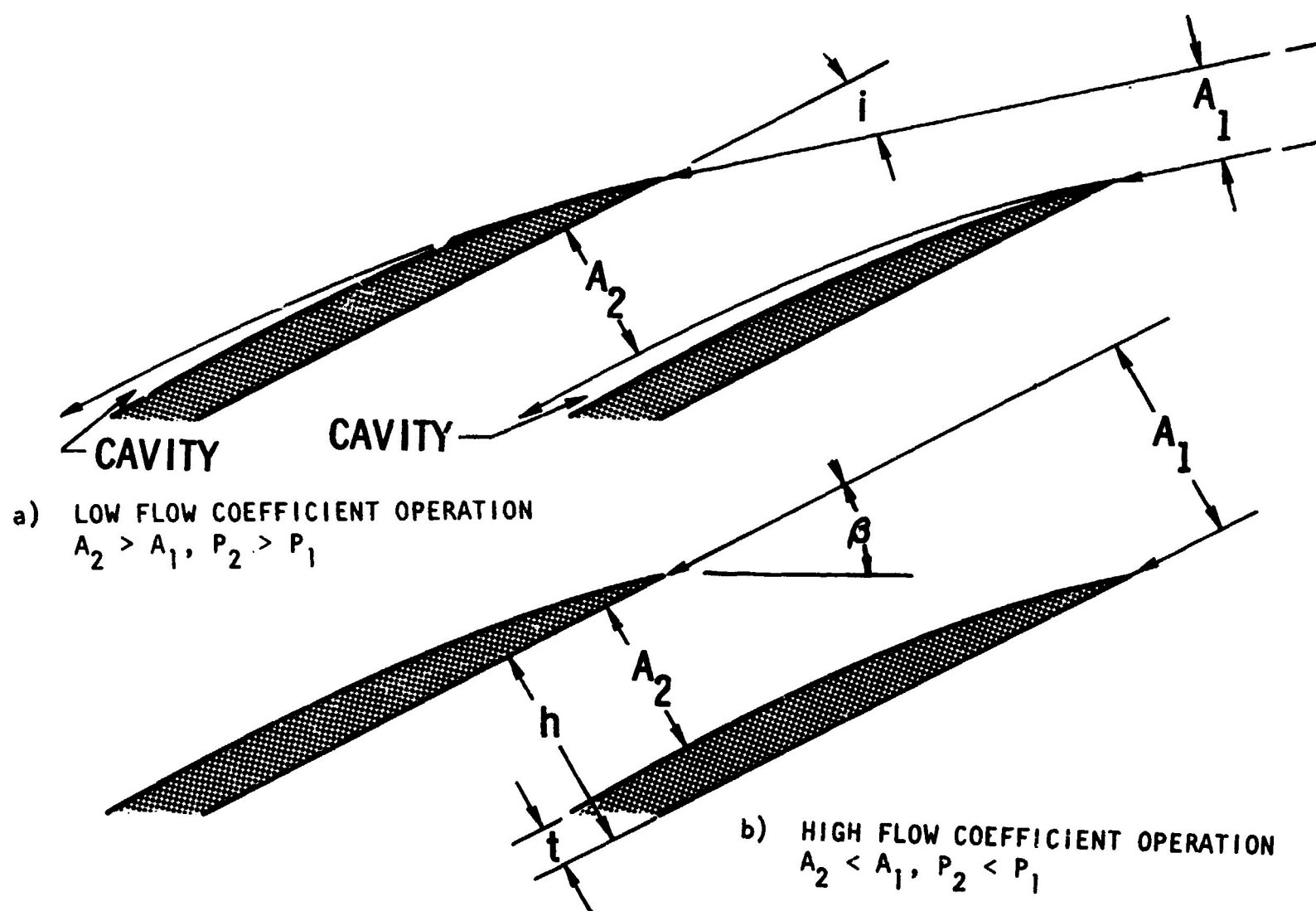


Figure 2. Effect of Flow Coefficient on Flow Geometry at Inducer Inlet

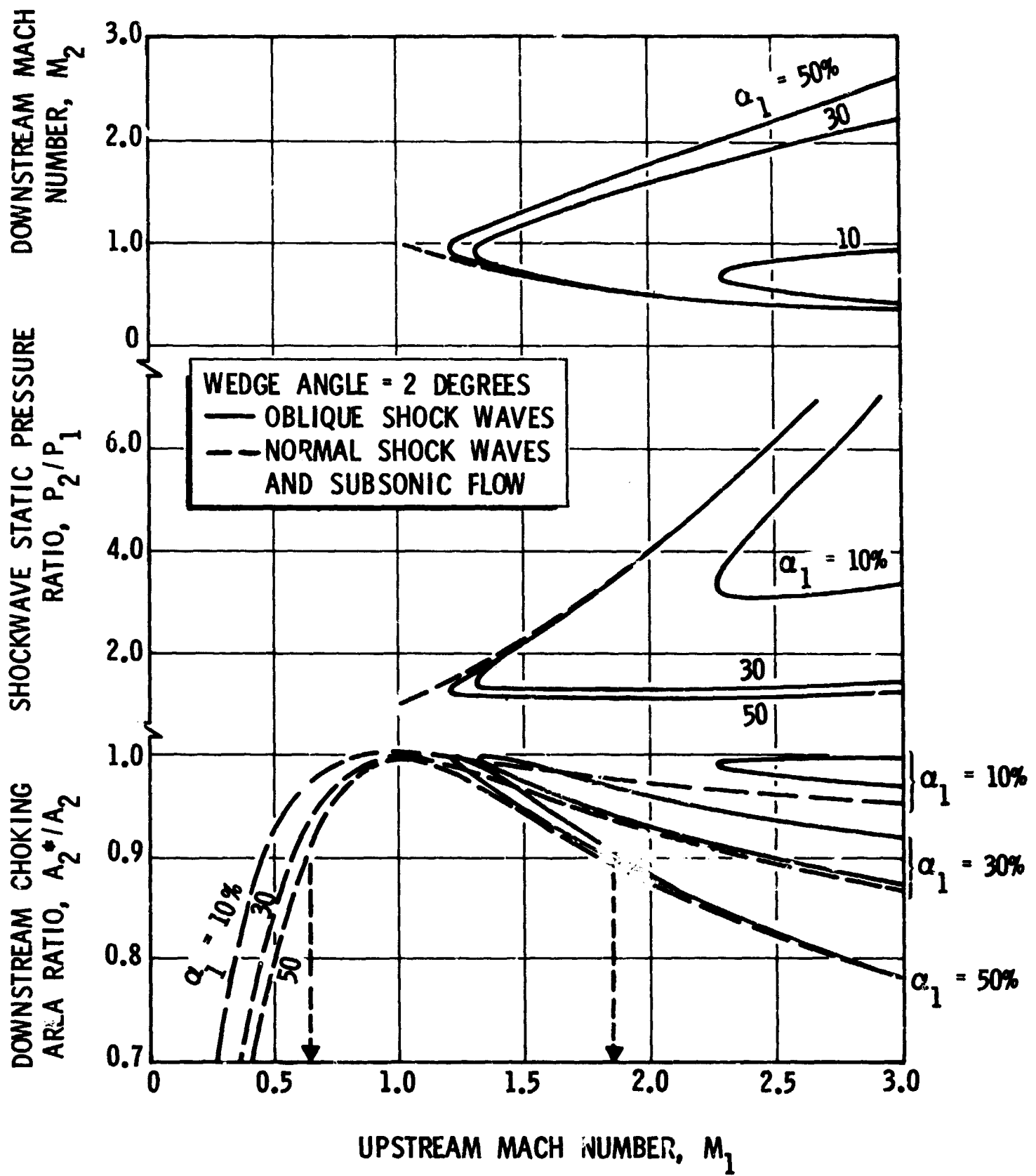


Figure 3. Effect of Vapor-Volume Fraction on Flow Parameters for Constant-Quality, Two-Phase Flow With an Isothermal Gas

to the upstream flow area,  $A_1$  (Fig. 2). Because the maximum vapor volume flowrate that can be pumped is equal to the difference between the mixture volume flowrate at the limit and the liquid volume flowrate, the vapor volume fraction at the choking limit may be approximated by Eq. 1 (Ref. 3).

$$\alpha_{1*} \approx 1 - \frac{1 - (i/\beta)_L}{1 - B} \quad (1)$$

Figure 4 is a plot of this relationship. As shown, the vapor pumping capacity may be increased by decreasing the inducer blade blockage (B), and by increasing the incidence-to-blade angle ratio ( $i/\beta$ ) that would exist if the flow were a pure liquid (Ref. 2, 3, and 5). Twenty percent head loss two-phase hydrogen test data for the Mark 15 and 25 hydrogen pumps are plotted on these curves in Fig. 4a and 4b, respectively. The indicated blockages of 32 and 24 percent show good agreement with the predicted values of 33 and 24 percent that are the result of adding 4 percent boundary layer blockage to measured blade blockages of 29 and 20 percent, respectively. It may be concluded that this approximate prediction has shown good agreement with previously obtained two-phase hydrogen test data. The triangular data in Fig. 4a did not correlate because they were limited by inlet line choking rather than by blade choking. Because the Mark 29  $O_2$  pump two-phase oxygen test data (Fig. 4c) are the product of this contract, they will be discussed later in this report under the data comparison section. It must be noted that all data shown in Fig. 4 were obtained from tests in which the flow passage area at the vapor fraction measuring station was equal to the inducer inlet annulus area and in which no flow was recirculated from a downstream location back through the inducer inlet. As a result, the vapor fractions shown are those that entered the inducers.

Equation 1 may be modified to express vapor fraction as a function of liquid flow coefficient rather than liquid incidence-to-blade angle ratio.

$$\alpha_{1*} \approx 1 - \frac{\phi_L}{\phi_{L-des}} \left[ \frac{1 - [(i/\beta)]_{L-des}}{1 - B} \right] \quad (2)$$

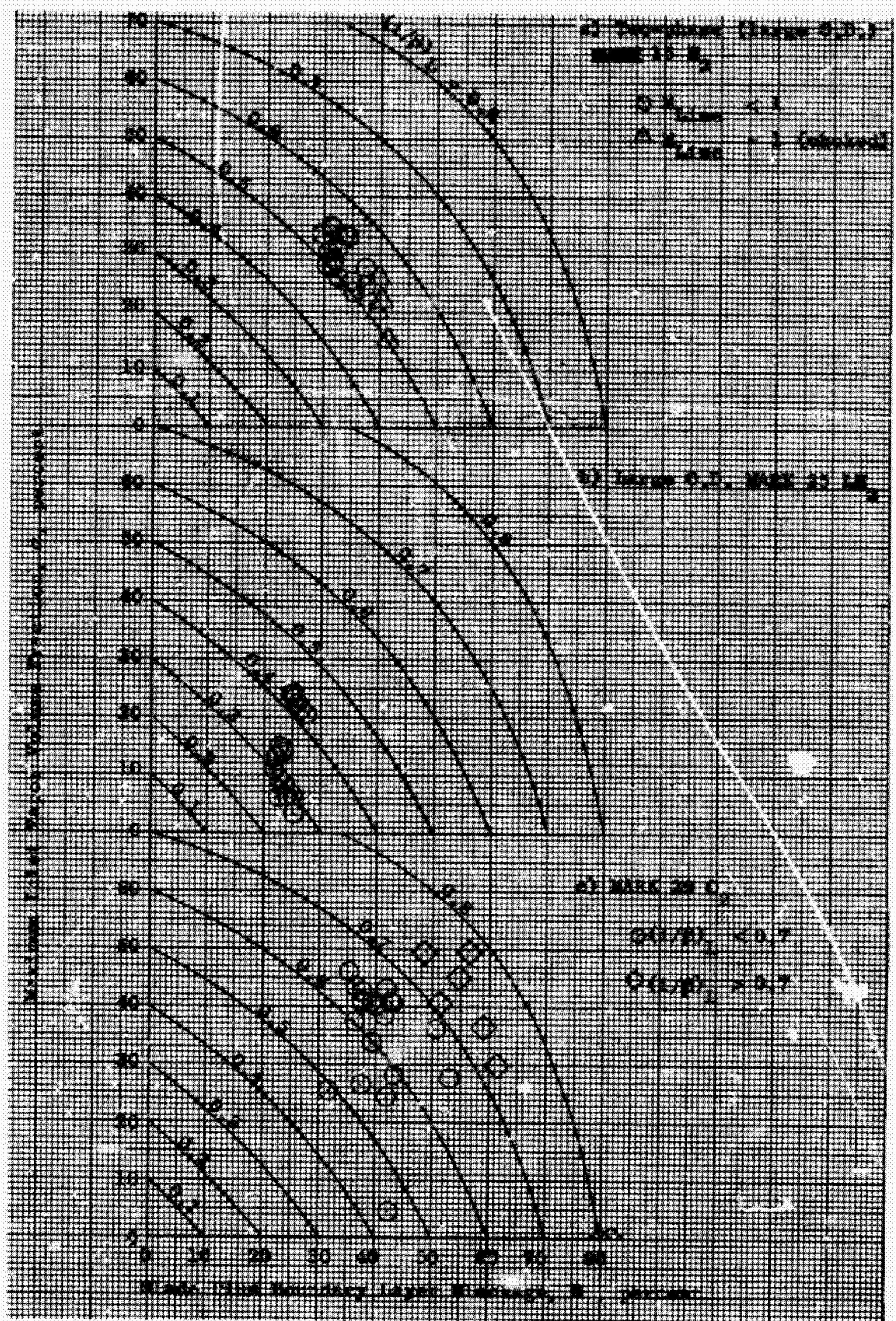


Figure 4. Determination of Inducer Flow Blockage From 20-Percent Head Loss Two-Phase Test Data

This relationship is compared with the 20-percent head loss data in Fig. 5. From the standpoint of relating the vapor pumping capability to the system, Fig. 5 is probably a better illustration of this limit than Fig. 4, which was blade geometry oriented.

Zero NPSH. Isentropic expansions from saturated liquid oxygen in the tank to equilibrium two-phase oxygen in the pump inlet line are shown in Fig. 6. This figure shows how increasing the vapor pumping capacity decreases the tank requirement. For example, with a pump inlet line flow per unit area of  $400 \text{ lb/sec ft}^2$ , increasing the vapor pumping capacity from 23 to 43 percent by volume decreases the tank (vapor) pressure requirement from 20 to 15 psia. In addition, it is shown that increasing the inlet line and the inducer inlet annulus areas will also decrease the tank pressure requirement. For example, at a vapor pumping capacity of 23 percent, increasing the inlet area to decrease the inlet flow per unit area from 400 to  $315 \text{ lb/sec ft}^2$  will also decrease the tank pressure requirement from 20 to 15 psia. It is apparent that, in addition to having minimum blade blockage and maximum incidence-to-blade angle ratio, a two-phase inducer should have a large inlet annulus area (Ref. 2, 3, and 5).

Inlet Line Choking Limit. Flow will choke when it reaches its acoustic velocity or, in other words, its point of maximum flow per unit area during an isentropic expansion. When this occurs, a decrease in inlet pressure will decrease the acoustic velocity (Fig. 1), thereby decreasing the flowrate allowed to pass into the pump. For zero NPSH, inlet line choking occurs at the peaks of the flow per unit area curves on Fig. 6 between vapor fractions of 60 and 70 percent. The acoustic velocity for an equilibrium flow process (Fig. 1) is a more universal indicator of inlet line choking because it applies to all NPSH's. Note that the pressures in Fig. 1 are static rather than total.



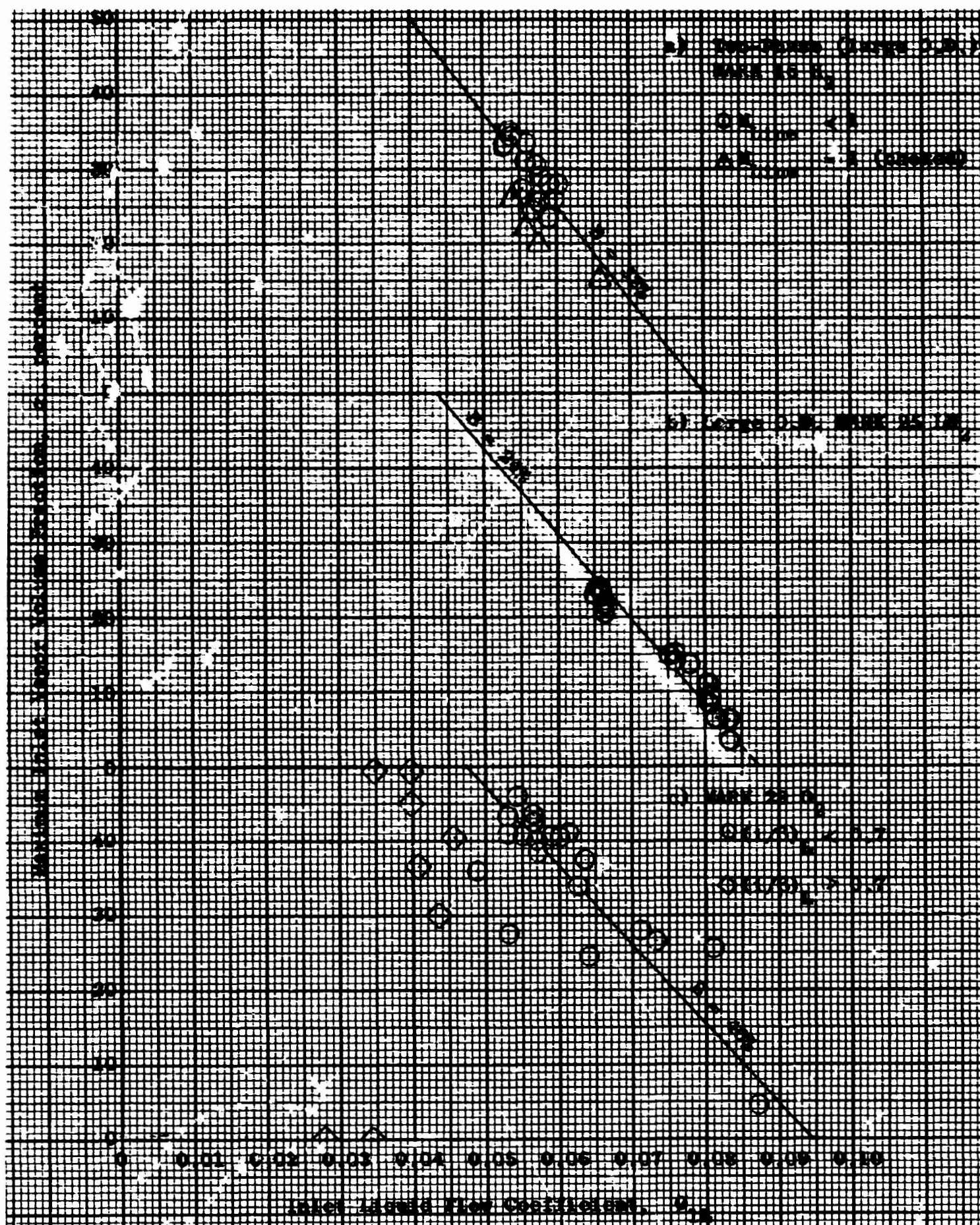


Figure 5. Comparison of Simplified Prediction With 20-Percent Head Loss Two-Phase Test Data

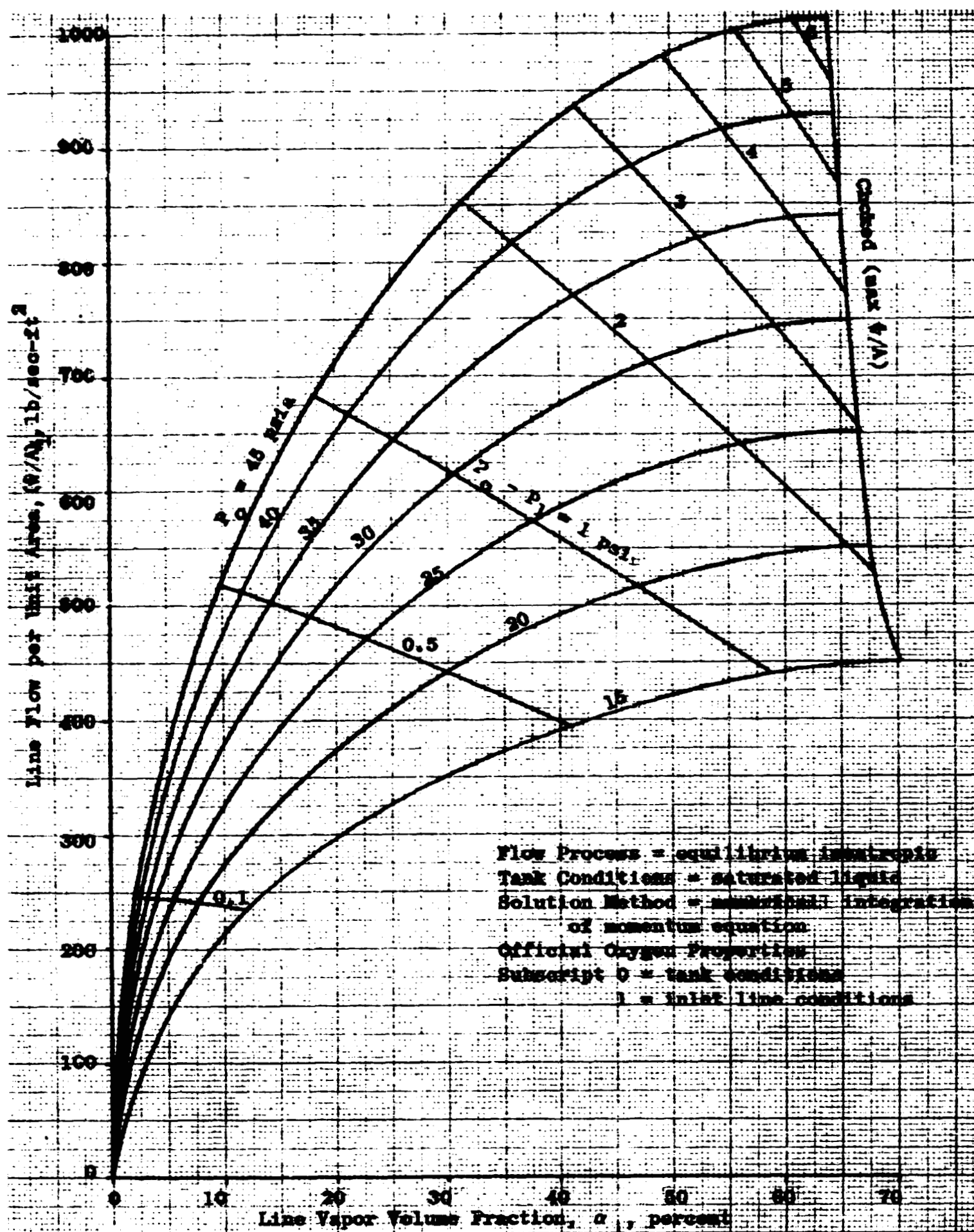


Figure 6. Oxygen Pump Inlet Size Requirements for Zero Tank NPSH Operation

### Model Analysis

The two-phase inducer mathematical model for hydrogen (Ref. 1) was rewritten so that it could be applied to oxygen as well as to hydrogen and could easily be converted to other fluids. In addition, the iterations within the program were changed to reduce computer time.

Fluid Properties. The equilibrium, isentropic flow process curves used to size pump inlets for zero tank NPSH operation (Fig. 6) were generated using two methods of solution and real oxygen properties from computer subroutines. The first method of solution analyzed was the enthalpy drop solution in which the isentropic enthalpy drop from the original saturated liquid condition at pressure  $P_0$  to the downstream pressure  $P_2$  is equal to the downstream kinetic energy.

$$V_2 = \sqrt{2 g J (h_0 - h_2)} \quad (3)$$

At low tank pressures ( $P_0 = 15$  psia), this solution produced  $\dot{W}/A$ 's that were much less than the approximate curves reported in Ref. 6. Therefore, a second method of solution was made in which the velocity was obtained by numerically integrating the momentum equation.

$$\int \frac{144 dP}{\rho} = - \int \frac{V dV}{g} \quad (4)$$

The numerical solution was made by assuming a linear variation in density over small increments in pressure. As a result, the velocity at pressure  $P_2$  is related to the conditions at the slightly greater upstream pressure  $P_1$  by Eq. 5.

$$V_2 = \sqrt{\frac{144 (2g) (P_1 - P_2)}{\rho_1 - \rho_2} \ln \frac{\rho_1}{\rho_2} + V_1^2} \quad (5)$$

where

$$P_1 = P_2 + \Delta P$$



The two methods of solution are compared at tank pressures of 15 and 45 psia in Fig. 7. At 15 psia, the enthalpy drop solution produces a W/A that is 30 to 40 percent less than the momentum solution. Therefore, a logarithmic differentiation of Eq. 3 and 5 was used to determine the sensitivity of velocity to errors in fluid properties. For the initial pressure increment as the flow leaves the tank ( $V_1 = 0$ ), Eq. 6 expresses the sensitivity of velocity to errors in liquid enthalpy for the enthalpy drop solution and Eq. 7 expresses the velocity sensitivity to errors in density for the momentum solution. These parameters were used because each produces the most velocity error in the respective solutions.

$$\frac{dV}{V} = \left[ \frac{-g J (1 - x) h_L}{V^2} \right] \frac{dh_L}{h_L} \quad (6)$$

$$\frac{dV}{V} = \frac{1}{2} \left[ \frac{1}{\frac{\rho_o}{\rho} - 1} - \frac{1}{\ln(\rho_o/\rho)} \right] \frac{d\rho}{\rho} \quad (7)$$

These equations were evaluated at 15 psia, thereby producing the relationships expressed by Eq. 8 and 9.

$$\frac{dh_L}{h_L} = -0.0000031 \frac{dV}{V} \quad (8)$$

$$\frac{d\rho}{\rho} = -4 \frac{dV}{V} \quad (9)$$

Therefore, to obtain 1-percent accuracy in velocity for the enthalpy drop solution, the error in liquid enthalpy can be no more than  $3.1 \times 10^{-8}$  percent (Eq. 8). For the momentum solution, 1-percent accuracy is obtained with 4-percent accuracy in density (Eq. 9). According to the National Bureau of Standards at Boulder, Colorado (the original source of oxygen property data), the average oxygen property accuracy at low temperature is 0.5 percent. It was concluded that the discrepancy shown in Fig. 7 was due to the extreme accuracy requirements of the enthalpy drop solution and that the momentum solution should be used for all equilibrium expansions of two-phase oxygen.

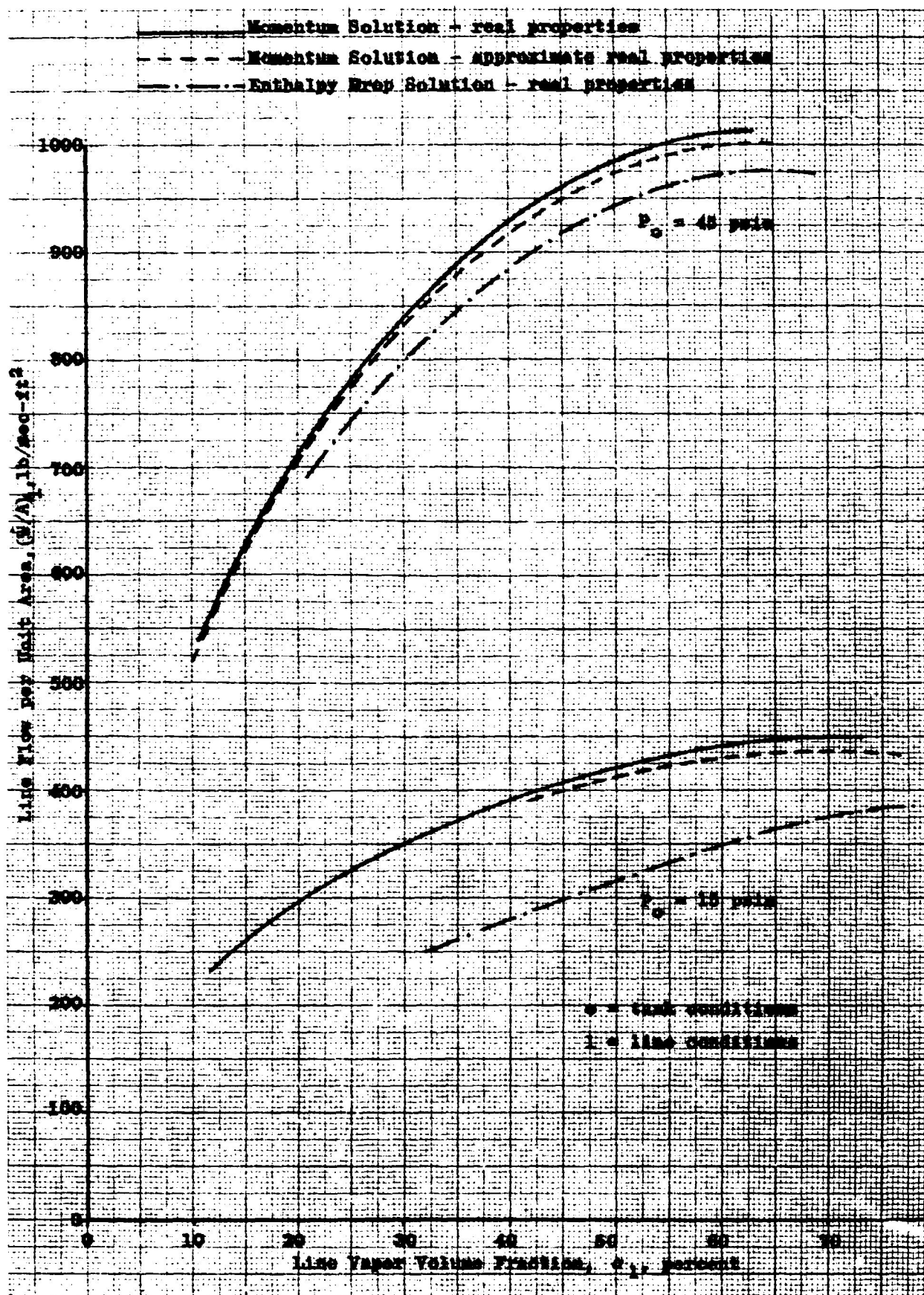


Figure 7. Comparison of Solution Methods for the Equilibrium Isentropic Flow of Oxygen under Zero Tank NPSH Conditions

Figure 6 shows the new W/A vs  $\alpha$  curves for sizing oxygen pump inlets for zero tank NPSH operation. Based on the preceding analysis, the numerical solution of the momentum equation was used to generate the data. This figure replaces the preliminary figure reported in Ref. 6.

Because the solution method had such a large effect in two-phase oxygen and because earlier analysis of two-phase hydrogen used the enthalpy drop solution, both solution methods and the real properties from computer subroutines were used to generate new W/A vs  $\alpha$  curves for hydrogen at zero tank NPSH. The results were found to be identical. It may be concluded that two-phase hydrogen is not as sensitive as two-phase oxygen to fluid properties.

The momentum solution was then incorporated into the two-phase oxygen test data reduction program. This procedure is described in Appendix A. Again, the two methods of solution were compared and, surprisingly, the solution method was found to make little difference. The fact that the solution method had a large effect when generating Fig. 6 but had no effect when reducing test data is not reasonable. This apparent discrepancy may reside in the computer programming, either in writing the program or in using the appropriate oxygen property subroutine.

The relationships used in the two-phase inducer mathematical model are based on simplified correlations to obtain approximate oxygen and hydrogen properties for inlet saturation pressures up to 60 psia. For oxygen, the W/A vs  $\alpha$  predictions using the approximate properties are compared with those using real properties in Fig. 7. The maximum errors shown are 3 and 1 percent at tank saturation pressures of 15 and 45 psia, respectively. The hydrogen property correlations used in the original hydrogen math model (Ref. 1) were modified to obtain better agreement with real property predictions.

Acoustic Velocities. Similar to the two-phase hydrogen inducer mathematical model (Ref. 1), a constant-quality flow process was assumed within the inducer flow passages because the high flow velocities will probably not allow sufficient time for significant amounts of condensation to occur within the

inducer blades. Because two-phase inducers typically operate at tip relative Mach numbers between 1 and 2 (Ref. 2 and 3), the acoustic velocity is an important parameter in the analysis of two-phase flow. Therefore, the acoustic velocity was used to evaluate the accuracy of simplifying assumptions for the constant-quality flow process.

Equation 10 describes the acoustic velocity in a two-phase fluid in which the liquid phase is incompressible and the gaseous phase is a perfect gas. Equations 11, 12, and 13 approximate the properties of oxygen gas that is near saturation and Eq. 14, 15, and 16 are the hydrogen gas approximations. The specific heats  $c_s$  and specific heat ratios  $\gamma_s$  are at constant entropy rather than the usual constant pressure because, near saturation, the enthalpies of these gases are strong functions of pressure as well as temperature.

$$c_x = \sqrt{\frac{x \gamma_s g R T}{\alpha}} \quad (10)$$

$$\text{O}_2 \quad \left\{ \begin{array}{l} R = \frac{144 P}{T (0.038 + 0.0166 P)} \\ c_s = \left( \frac{\partial h}{\partial T} \right)_s = 0.220 - 0.000469 P \\ \gamma_s = \frac{1}{1 - R/C_s J} \end{array} \right. \quad (11)$$

$$(12)$$

$$(13)$$

$$\text{H}_2 \quad \left\{ \begin{array}{l} R = \frac{144 P}{T (0.009 + 0.005075 P)} \\ c_s = 2.30 - 0.0098 P \\ \gamma_s = \frac{1}{1 - R/C_s J} \end{array} \right. \quad (14)$$

$$(15)$$

$$(16)$$

The approximate constant-quality acoustic velocities (from Eq. 10 through 16) are compared with the real values (Ref. 7 and 8) in Fig. 8. For oxygen, the maximum errors are 4 and 0 percent at pressures of 15 and 45 psia, respectively. For hydrogen, the corresponding maximum errors are 5 and 4 percent. Because

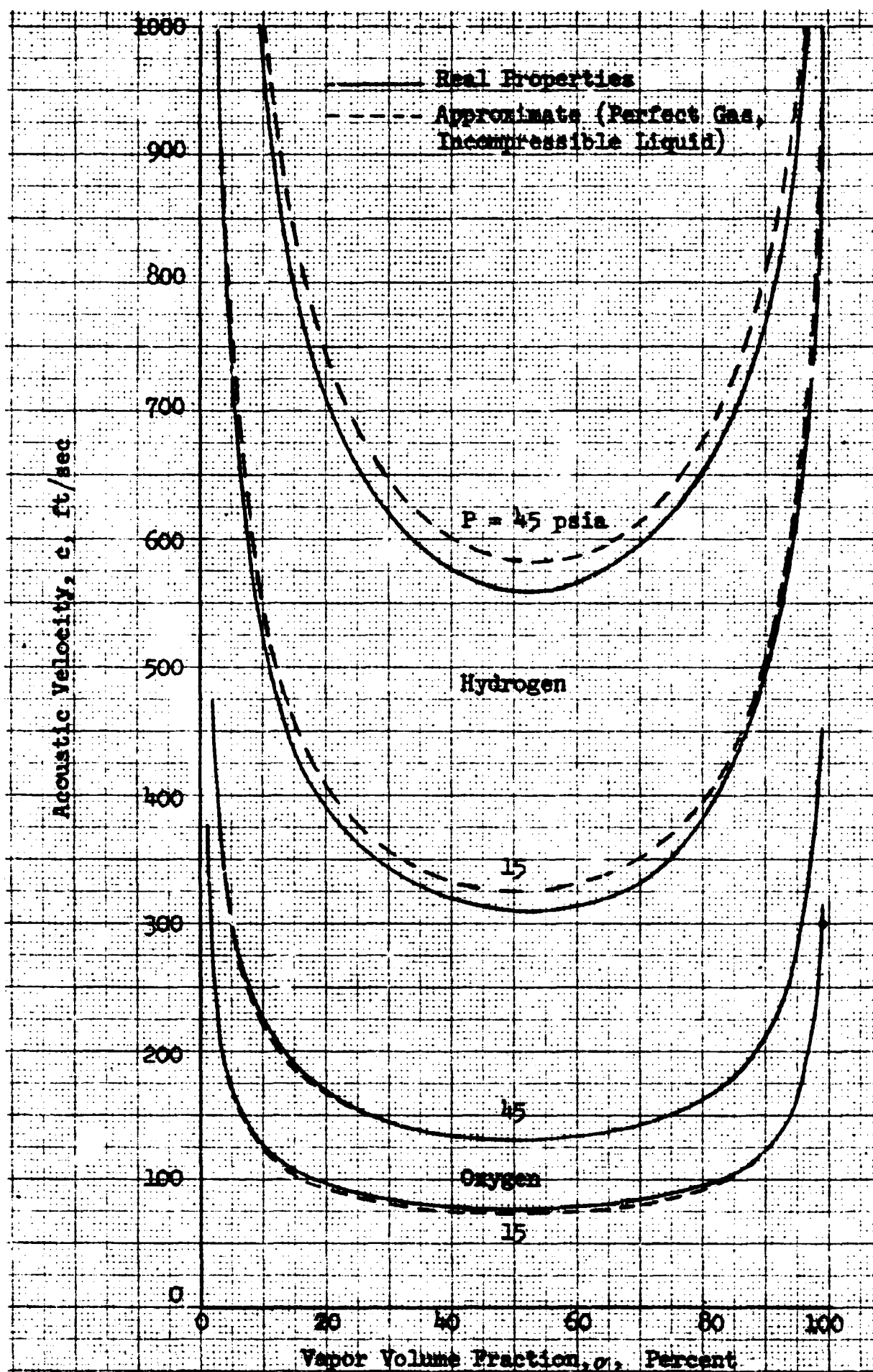


Figure 8. Evaluation of Acoustic Velocity Prediction for Constant-Quality, Two-Phase Flow

the agreement is satisfactory, the assumptions of an incompressible liquid phase and a perfect gaseous phase were used in the mathematical model to describe the flow within the inducer blade passages. The gas properties are calculated (Eq. 11 through 13 for  $O_2$  and 14 through 16 for  $H_2$ ) at the inducer inlet and are held constant as the flow passes downstream of the inlet.

The constant-quality, two-phase acoustic velocities (Eq. 10 through 16) are important to two-phase inducer design because they strongly influence the inducer pressure rise and the pressure distribution within the inducer flow passages. Therefore, they are shown in Fig. 1 for hydrogen and oxygen at saturation pressures of 15, 30 and 45 psia. Similar to hydrogen, the oxygen values reach a minimum at approximately 50 percent vapor by volume and increase approximately with the square root of the static pressure. However, for a given static pressure and vapor volume fraction, the oxygen constant-quality acoustic velocities are 23 percent of those for hydrogen. It is of interest to note that this value is approximately equal to the square root of the hydrogen-to-oxygen density ratio. These observations may prove to be of value in relating the two-phase performance in one fluid to that in another fluid.

To determine the flow conditions under which equilibrium two-phase oxygen and hydrogen will choke in the inlet line, the acoustic velocities were predicted using Eq. 17 and the simplified property correlations, the accuracies of which are shown in Fig. 7 for oxygen.

$$c = \sqrt{144 \left( \frac{\partial P}{\partial \rho} \right)_s} = \sqrt{\frac{1 + x \left( \frac{\rho_L}{\rho_V} - 1 \right)}{144g \left( \frac{\partial P}{\partial \rho_L} \right)_s + \frac{x (\rho_L/\rho_V)^2}{144g \left( \frac{\partial P}{\partial \rho_V} \right)_s} - \frac{\rho_L}{144g} \left( \frac{\rho_L}{\rho_V} - 1 \right) \left( \frac{\partial x}{\partial P} \right)_s}} \quad (17)$$

The partial derivatives at constant entropy in Eq. 17 refer to constant two-phase entropy rather than to constant entropy in the pure phases because the flow process is in equilibrium. In oxygen, the resulting acoustic velocities are very small, reaching minimum values of 6.9 and 17.9 ft/sec at pressures

of 15 and 45 psia, respectively (Fig. 1). In hydrogen, the corresponding minimum values are 60 and 145 ft/sec, respectively. In both propellants, these minimum values occur as the vapor volume fractions approach zero. It is therefore apparent that, in equilibrium flow, a discontinuity in acoustic velocity exists at the liquid saturation line because the pure liquid acoustic velocities are many times the minimum two-phase values. It is also apparent that two-phase oxygen will choke in the inlet line at a velocity that is approximately 12 percent of the hydrogen value. Figure 1 also shows that equilibrium acoustic velocity is affected more by static pressure than constant-quality acoustic velocity.

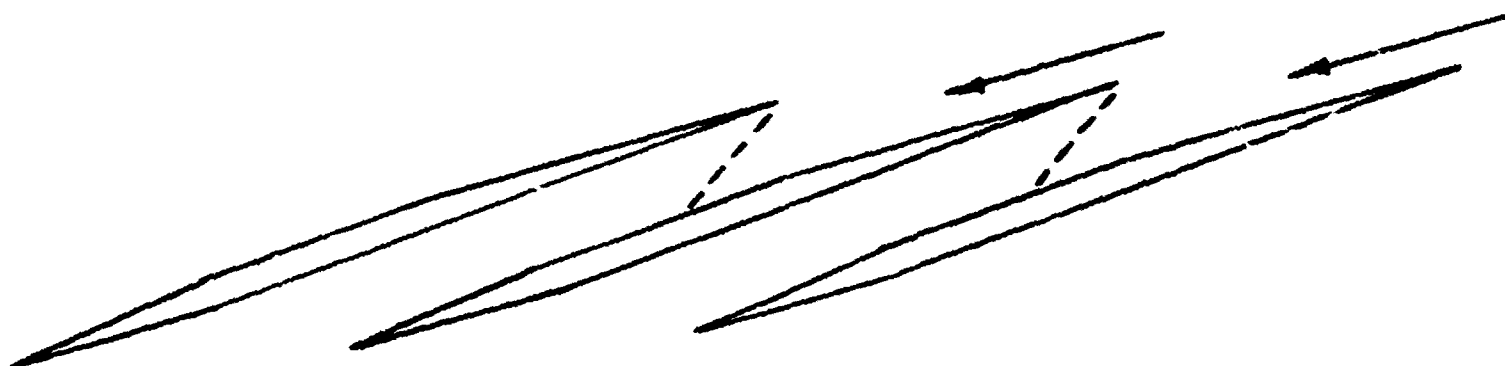
Computer Program. The two-phase oxygen inducer mathematical model was selected from the nine flow models investigated under the two-phase hydrogen inducer program (Ref. 1). To provide background for this selection, these nine models are reviewed below.

These models are listed along with their assumed flow patterns in Table 1. For the first seven models listed, a constant-quality flow process was assumed throughout the inducer under all flow conditions. For the next two models (MOD-6PG and MOD-7PG), a constant-quality flow process was assumed down to the throat (point of blade overlap) under all flow conditions and, downstream of the throat, the flow deviated from constant quality only during certain supersonic operating conditions. For the first four models listed, an isothermal gas ( $\rho_v/P = \text{constant}$ ) was assumed. A perfect gas ( $\rho_v/P^{1/\gamma} = \text{constant}$ ) was assumed for the remaining seven models. The shock wave patterns for the first six models listed are shown in Fig. 9, and the basic computer flow chart for the various models is shown in Fig. 10. For the four isothermal gas models, the effects of inlet vapor volume fraction on the static pressure rise across a typical flat plate hydrogen inducer are shown in Fig. 11 for a typical supersonic operating point at an inlet static pressure of 30 psia. For the next five perfect gas models, the corresponding characteristics are shown in Fig. 12 for inlet static pressures of 30 and 15 psia. The characteristics of these various models are discussed below, first for subsonic operation and then for supersonic operation.

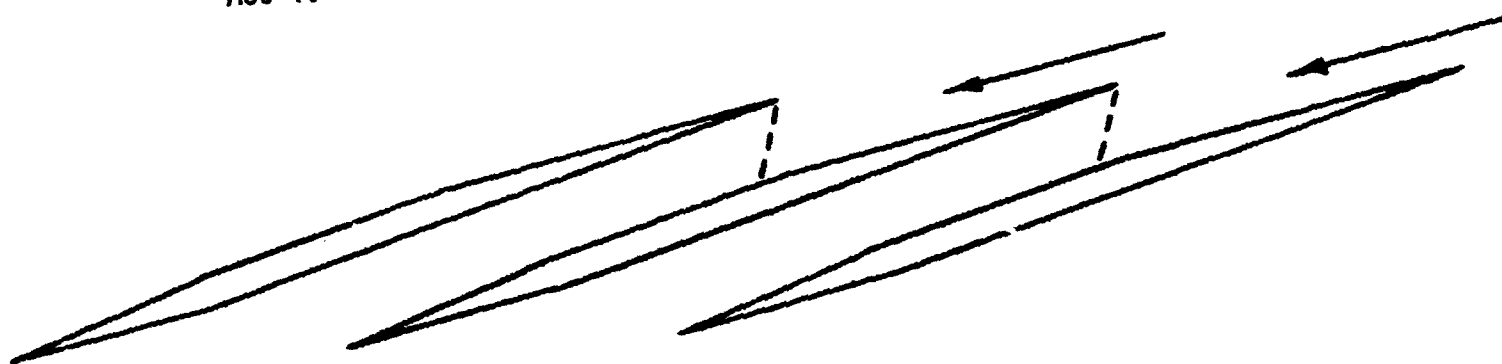


TABLE 1. FLOW MODELS INVESTIGATED

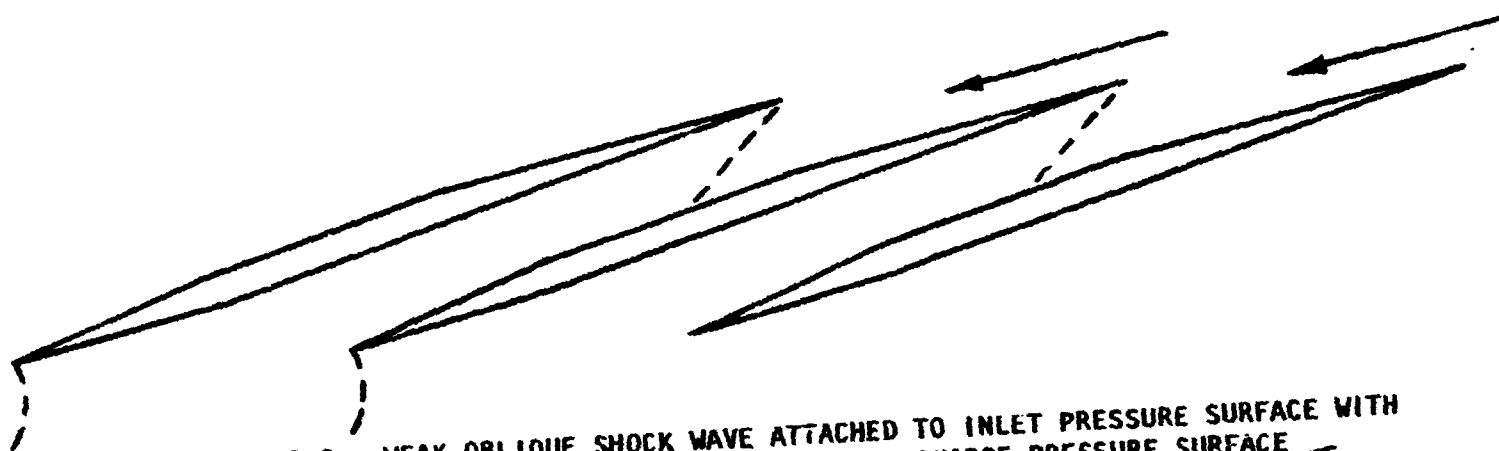
| Model               | Gaseous Phase | Oblique Shock Wave Type<br>(Normal if $M_1 < M_{1min}$ ) | Assumed Normal Shocks      | Flow Condition After Normal Shock                                         | Vapor Cavity   |  |  |
|---------------------|---------------|----------------------------------------------------------|----------------------------|---------------------------------------------------------------------------|----------------|--|--|
| MOD-1T              | Isothermal    | Weak                                                     | None                       | Two-Phase                                                                 | None           |  |  |
| MOD-2T              |               | Strong                                                   | None                       |                                                                           |                |  |  |
| MOD-3T              |               | Weak                                                     | Discharge Pressure Surface |                                                                           |                |  |  |
| MOD-4T              | Perfect       | None                                                     | Inlet Suction Surface      | Liquid<br>Liquid if $A_t > A_1$<br>Liquid if $A_t > A_1$<br><br>Two-Phase | If $A_t > A_1$ |  |  |
| MOD-1PG             |               | Weak                                                     | None                       |                                                                           |                |  |  |
| MOD-2PG             |               | Strong                                                   |                            |                                                                           |                |  |  |
| MOD-5PG             |               | Strong                                                   |                            |                                                                           |                |  |  |
| MOD-6PG             |               | Weak                                                     |                            |                                                                           |                |  |  |
| MOD-7PG             |               | Weak                                                     |                            |                                                                           |                |  |  |
| High Head ( $H_2$ ) |               | Weak                                                     |                            |                                                                           |                |  |  |
| Low Head ( $O_2$ )  |               | Weak                                                     |                            |                                                                           |                |  |  |



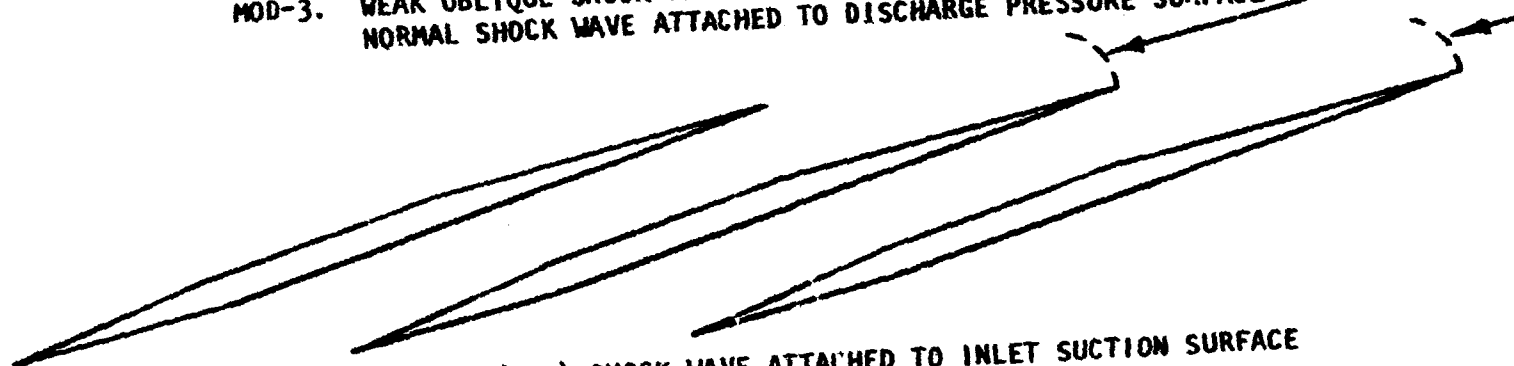
MOD-1. WEAK OBLIQUE SHOCK WAVE ATTACHED TO INLET PRESSURE SURFACE



MOD-2. STRONG OBLIQUE SHOCK WAVE ATTACHED TO INLET PRESSURE SURFACE



MOD-3. WEAK OBLIQUE SHOCK WAVE ATTACHED TO INLET PRESSURE SURFACE WITH  
NORMAL SHOCK WAVE ATTACHED TO DISCHARGE PRESSURE SURFACE



MOD-4. NORMAL (BOW) SHOCK WAVE ATTACHED TO INLET SUCTION SURFACE

Figure 9. Supersonic Shock Wave Patterns for Two-Phase  
Hydrogen Inducer Flow Models

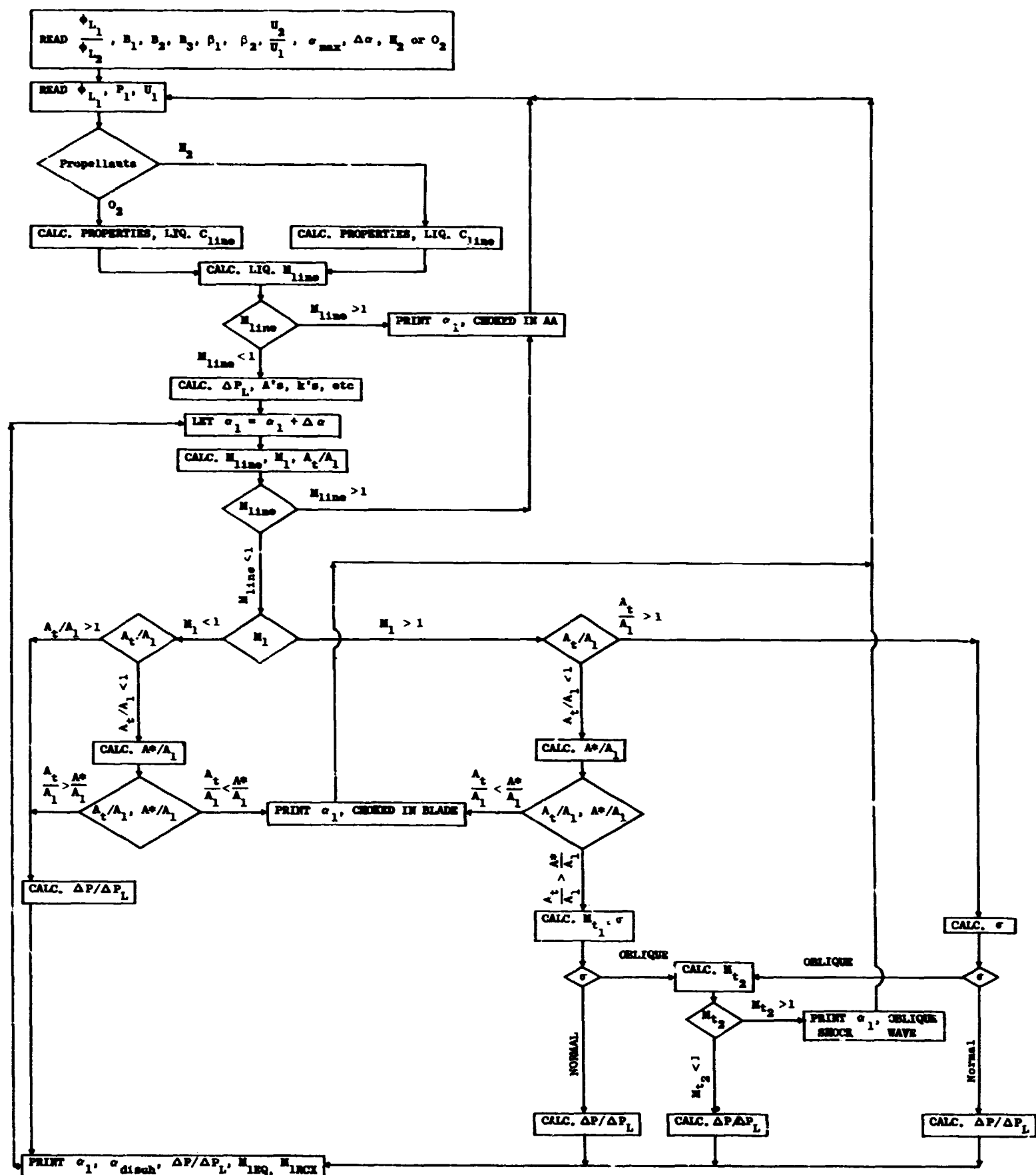


Figure 10. Computer Flow Chart for Two-Phase Inducer Mathematic. Model

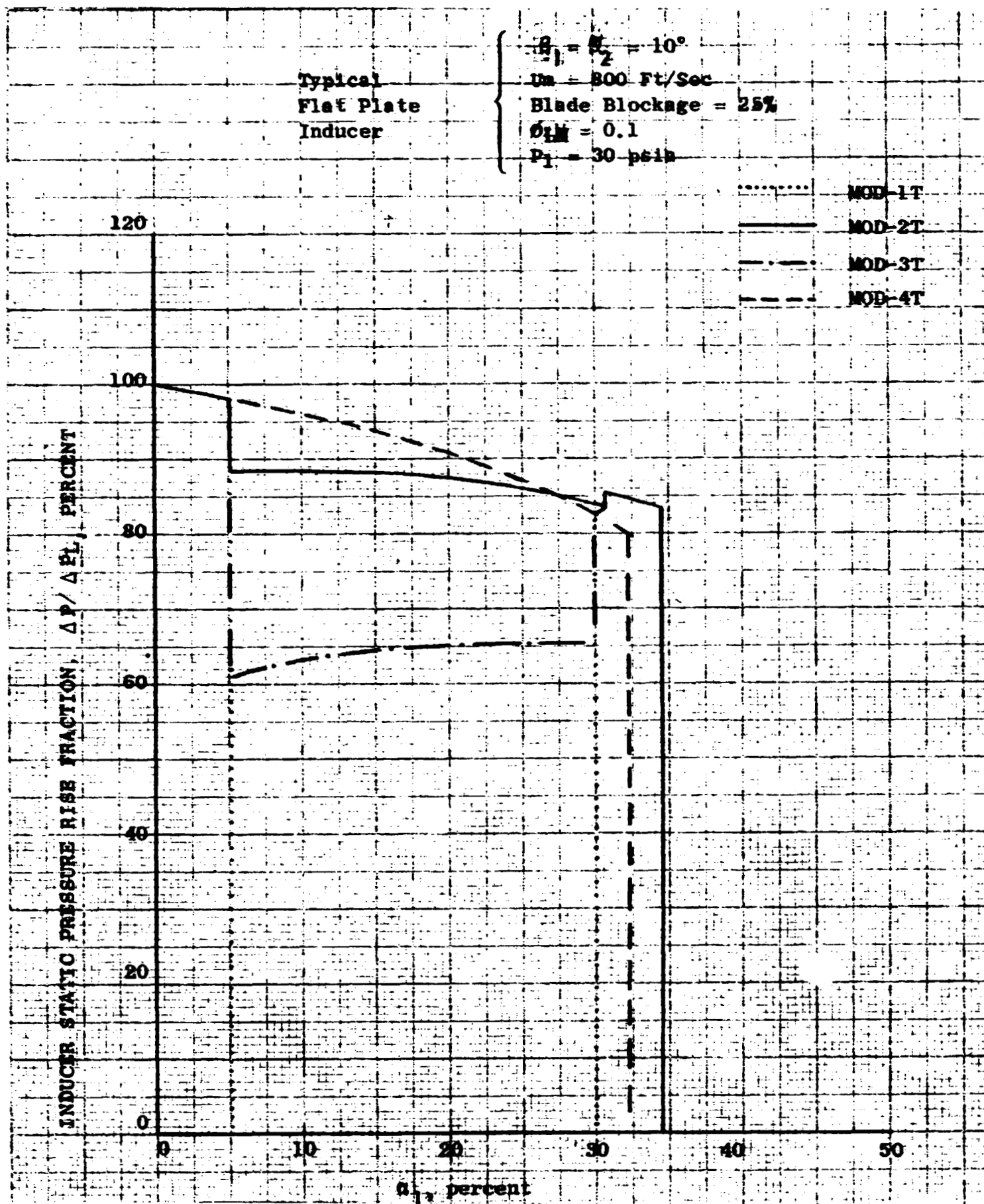


Figure 11. Flow Model Comparison--Isothermal Gas

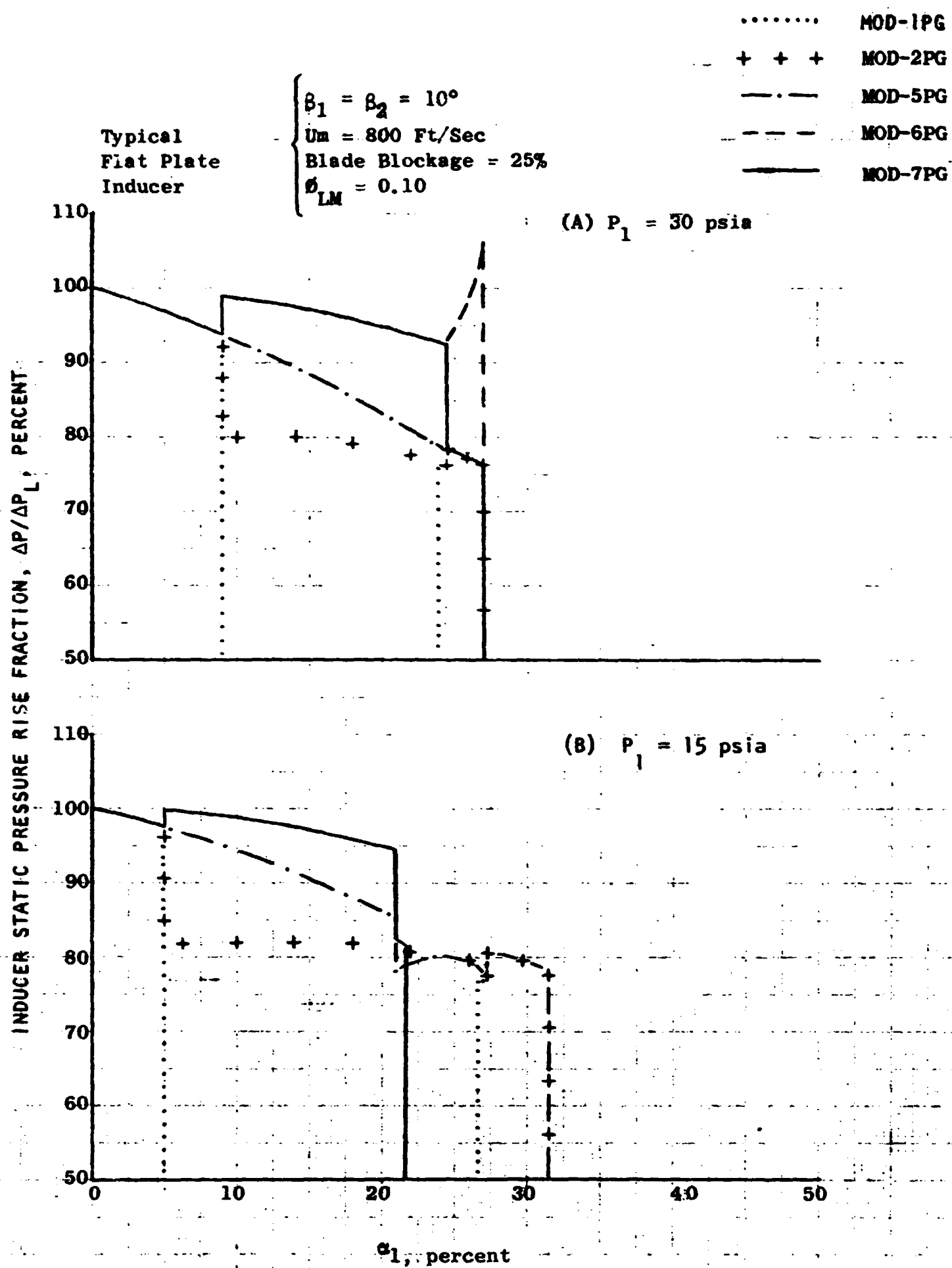


Figure 12. Flow Model Comparison--Perfect Gas

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Subsonic Operation. During subsonic operation, the assumed flow processes and flow patterns are identical for the four isothermal gas flow models and, therefore, the predicted performances are identical. The same is true for the five perfect gas flow models. The difference between the two basic characteristics is that the fluid with the more compressible isothermal gas has a lower acoustic velocity and, therefore, for a given blade velocity, has a higher blade relative Mach number than the fluid with the perfect gas. As a result, for a given subsonic blade velocity and a given vapor volume fraction, the isothermal gas models have a higher static pressure rise. In addition, they have a lower vapor fraction at the choking limit. The general shapes of the static pressure rise vs vapor volume fraction curves for this type of operation are similar to the curve for MOD-4T in Fig. 11.

For low flow coefficient operation (positive incidence angle, Fig. 2a), the flow passage area increases continuously with distance from the inducer inlet and therefore, the flow will diffuse continuously as it passes through the inducer. For high flow coefficient operation (negative incidence angle, Fig. 2b), the flow will initially accelerate until the point of blade overlap (minimum flow area  $A_2$ ) is reached, and then will diffuse as it passes through the remainder of the inducer. However, if the flow reaches its acoustic velocity before reaching the point of blade overlap, it will be unable to pass through or, in other words, it will be choked. This choking limit is the primary limit that can occur during subsonic operation. The other possible limit occurs during very high flow coefficient operation with very low inlet Mach numbers. Under these circumstances, the flow will not choke and, therefore, a static pressure rise will be produced until the inlet flow coefficient becomes so high that the discharge flow passage area is less than the inlet. When this occurs, the static pressure drops rather than rises.

Supersonic Operation. During supersonic operation, several shock wave patterns are possible. The reasons for this are that: (1) oblique shock waves can be either weak or strong (Ref. 9), (2) upstream propagation of shock waves attached to inducer blade suction surfaces may occur, (3) normal shockwaves

may occur in the diverging portions of the flow passages, and (4) vapor cavities may occur following flow passage discontinuities. Consequently, nine mathematical models were developed to cover the range of possibilities that may occur during supersonic operation. The basic shock wave patterns for the models are illustrated schematically in Fig. 9. Each model is discussed individually below.

MOD-1T. The flow characteristic for Model No. 1 remains in the subsonic flow regime (Fig. 11) until the vapor fraction increases enough to drop the acoustic velocity down to the blade speed (Fig. 1). At this point, the area ratio is still quite divergent (Fig. 2a) and, therefore, the flow expands to a highly supersonic condition at the point of blade overlap. Because the Mach number is high, the shockwave attached to the blade inlet pressure surface (Fig. 9) is oblique (Fig. 3). As weak oblique shockwaves were assumed, the downstream flow is supersonic and, therefore, continues to expand to lower static pressures as it passes on to the discharge. The net result is a static pressure drop across the inducer as soon as the inlet relative Mach number reaches 1. For the conditions indicated in Fig. 11, this occurs at a vapor fraction of 5 percent. Recovery is indicated at a much higher vapor fraction. However, this recovery condition could never be reached at a high blade speed of 800 ft/sec (in hydrogen).

MOD-2T. For Model No. 2, strong oblique shockwaves were assumed, rather than weak (Fig. 9). Therefore, as shown in Fig. 11, the inducer head characteristic drops off at approximately 5 percent vapor fraction as did Model No. 1. However, because the flow downstream of strong oblique shock waves is subsonic, the downstream flow is diffused as it passes on to the inducer discharge. As a result, the static pressure rise drops only 10 rather than 100 percent. As shown in Fig. 11, the flow characteristic remains in the oblique shock wave flow regime until the flow area begins to converge (Fig. 2b). This drops the Mach number at the point of blade overlap such that a normal rather than an oblique shock wave is produced (Fig. 3). This causes the characteristic on Fig. 11 to rise slightly at a vapor fraction of 30 percent. This normal shock flow regime then continues until choking occurs (at 35 percent vapor on Fig. 11).



For supersonic flow at high flow coefficients (negative incidence angle, Fig. 2b) the flow encounters a positive wedge angle which, for an isolated airfoil, would cause a shock wave attached to the suction surface to occur. For a cascade of blades (i.e., nonisolated airfoils) with no curvature of the suction surfaces, each blade should create an oblique shock wave that will propagate upstream of the inducer inlet. Under these circumstances, the flow would encounter a succession of shock waves before reaching the inducer blade and, therefore, each wave should be quite weak. Consequently, it was assumed that supersonic flow would undergo essentially an isentropic compression upstream of the blades during high flow coefficient operation. Therefore, for Model No. 2, the only shock waves treated are those attached to the pressure side of the inducer blade leading edge.

MOD-3T. This is Model No. 1 with an additional normal shock wave located at the inducer discharge (third flow pattern in Fig. 9). As shown in Fig. 11, this model results in a static pressure rise in the oblique shock flow regime that is greater than Model No. 1 and less than Model No. 2. Also shown is a slight step in the curve at 30 percent vapor just before the normal shock flow regime occurs. This step is caused by the fact that weak oblique shock waves will have subsonic downstream Mach numbers if the upstream Mach numbers are getting close to the normal shock regime (Fig. 3).

MOD-4T. Another possible flow pattern that occurs during supersonic operation is one in which a bow wave stands upstream of the blade leading edge and extends over to the suction side of the blade (bottom flow pattern in Fig. 9). This flow pattern is simulated in Model No. 4 by assuming a normal shock wave attached to the suction surface of the inducer blade leading edge. In addition, it was assumed that this normal shock wave does not propagate upstream or, in other words, does not cross over into the flow stream tube upstream of the adjacent blade flow passage.

Because the flow downstream of a normal shock wave is subsonic and as normal shock waves do not change the direction of the flow, the characteristics of Model No. 4 in the supersonic flow regime are very similar to those for any of the models in the subsonic flow regime. The primary limit occurs at high flow coefficients when the resulting area contraction upstream of blade overlap causes choking. As shown in Fig. 11, the choking vapor fraction is lower for this model than for the three preceding models.

MOD-1PG. The difference between this model and MOD-1T is that the gaseous phase was assumed to be perfect rather than isothermal. As a result, the acoustic velocities are higher and, therefore, the relative Mach number for a given blade speed is lower. This causes the characteristic to fall off at a vapor fraction of 9 percent (Fig. 12a) rather than at 5 percent (Fig. 11).

MOD-2PG. This model is MOD-2T with a perfect rather than an isothermal gaseous phase. As with MOD-1PG, this causes the initial drop to occur at 9 percent vapor (Fig. 12a) rather than 5 percent vapor (Fig. 11). In addition, this causes choking to occur at a lower vapor fraction (27 percent on Fig. 12a) than with MOD-2T (35 percent on Fig. 11). This model indicates a higher choking limit at lower inlet pressures (Fig. 12b). This is caused by the fact that decreasing the inlet pressure decreases the acoustic velocity (Fig. 1), thereby increasing the inlet relative Mach number.

MOD-5PG. This model is MOD-2PG with a vapor cavity assumed whenever the blade suction surface pulls away from the approaching flow (Fig. 2a) or, in other words, when the geometrical downstream area is larger than the approach area. This cavity is assumed to be wedge shaped such that the flow is not deflected until it strikes the blade pressure surface at the point of blade overlap. As a result, the flow approaches the pressure surface at a lower Mach number and a higher angle than with MOD-2PG. This causes a normal shock wave to occur over much of what was the oblique shock flow regime of MOD-2PG. This eliminates the sudden dropoff in static pressure rise at a relative Mach number of 1 (Fig. 12).

MOD-6PG. This model is MOD-5PG with two added assumptions: (1) the flow condenses to a liquid whenever a normal shock occurs, and (2) whenever an oblique shock wave occurs, it is weak rather than strong. The first assumption causes the static pressure to rise at a relative Mach number of 1 (Fig. 12). The second assumption eliminates the excessively high vapor limit at low inducer inlet pressures (Fig. 12b, the curve is the same as for MOD-7PG). The first assumption also causes a peculiar sudden increase in the pressure rise as the vapor limit is approached for the 30-psia inlet pressure operating condition (Fig. 12a). This increase occurred in the region in which the area converged (Fig. 2b).

MOD-7PG. This model is MOD-6PG with one added assumption; the flow does not condense to a liquid across a normal shock wave if the area is converging as shown in Fig. 2b. This is probably a reasonable assumption because any area convergence reduces the Mach number approaching the shock wave and, therefore, reduces the static pressure rise across the shock. This eliminated the peculiarity of MOD-6PG at the 30-psia inlet pressure (Fig. 12a).

Hydrogen Model Selection. As shown in Table 1 and explained in Ref. 1, MOD-7PG was the mathematical model selected for predicting the performances of high head inducers in two-phase hydrogen. One of the reasons for this selection was that test data indicated very little effect of blade speed on vapor pumping capacity. MOD-1T, MOD-1PG, MOD-6PG, and MOD-7PG all had this characteristic whereas the other models did not. MOD-1T and MOD-1PG were eliminated because their predicted vapor limits were too low, thereby leaving MOD-6PG and MOD-7PG as the primary candidates. As shown in Fig. 12, these two models showed less effect of vapor fraction on pressure rise than the other models. As shown in Fig. 13, this characteristic was also indicated by the test data. MOD-7PG was finally selected because, as shown in Fig. 12a, MOD-6PG occasionally predicted an upward spike in pressure rise just before reaching the vapor pumping limit, a characteristic that has not been observed in the test data.

MOD-7PG is compared with hydrogen test data in Fig. 13. As shown, the correlation is reasonably good. The test pressure rises in Fig. 13 fell off more rapidly with increasing vapor fraction than did the model predictions. The probable reason for this is that the inlet line Mach number was approaching 1 even at zero vapor fraction and, therefore, some effect of choking was evident throughout the run. The indicated differences between the original predictions from Ref. 1 and the prediction of the computer program developed under the present contract are due to the recorrelation of the hydrogen properties. These differences are also shown in the vapor pumping limit comparison in Fig. 14. Also shown in Fig. 14 is that the new computer program does not predict as large a step at the transition from the oblique shock wave limit to the blade choking limit.

Oxygen Model Selection. As shown in Table 1, the model selected for two-phase oxygen differs from the hydrogen model in that the assumption of condensation to a pure liquid across a normal shock wave was removed for oxygen. As will be shown in the data comparison section of this report, the reason for this change is that the oxygen inducer pressure rises fell off more with increasing vapor fraction than did the hydrogen inducers. A possible explanation for this difference is that the hydrogen inducer was a high-head design with a head coefficient  $\psi$  of approximately 0.45, whereas the oxygen inducer is a "flat-plate," low-head design with a  $\psi$  of approximately 0.1. More condensation is likely in a high-head design because the pressure rises are greater and there is more time for condensation to occur before reaching the inducer discharge. Another possible explanation is that the ratio of the constant-quality acoustic velocity to the equilibrium acoustic velocity is much greater in oxygen than in hydrogen. Therefore, at the same inlet line Mach number, the blade relative Mach number is less in oxygen than in hydrogen and, as a result, the normal shock waves are not as strong in oxygen as in hydrogen. For a more complete discussion of the oxygen model and its comparison with test data, see the data comparison section of this report.

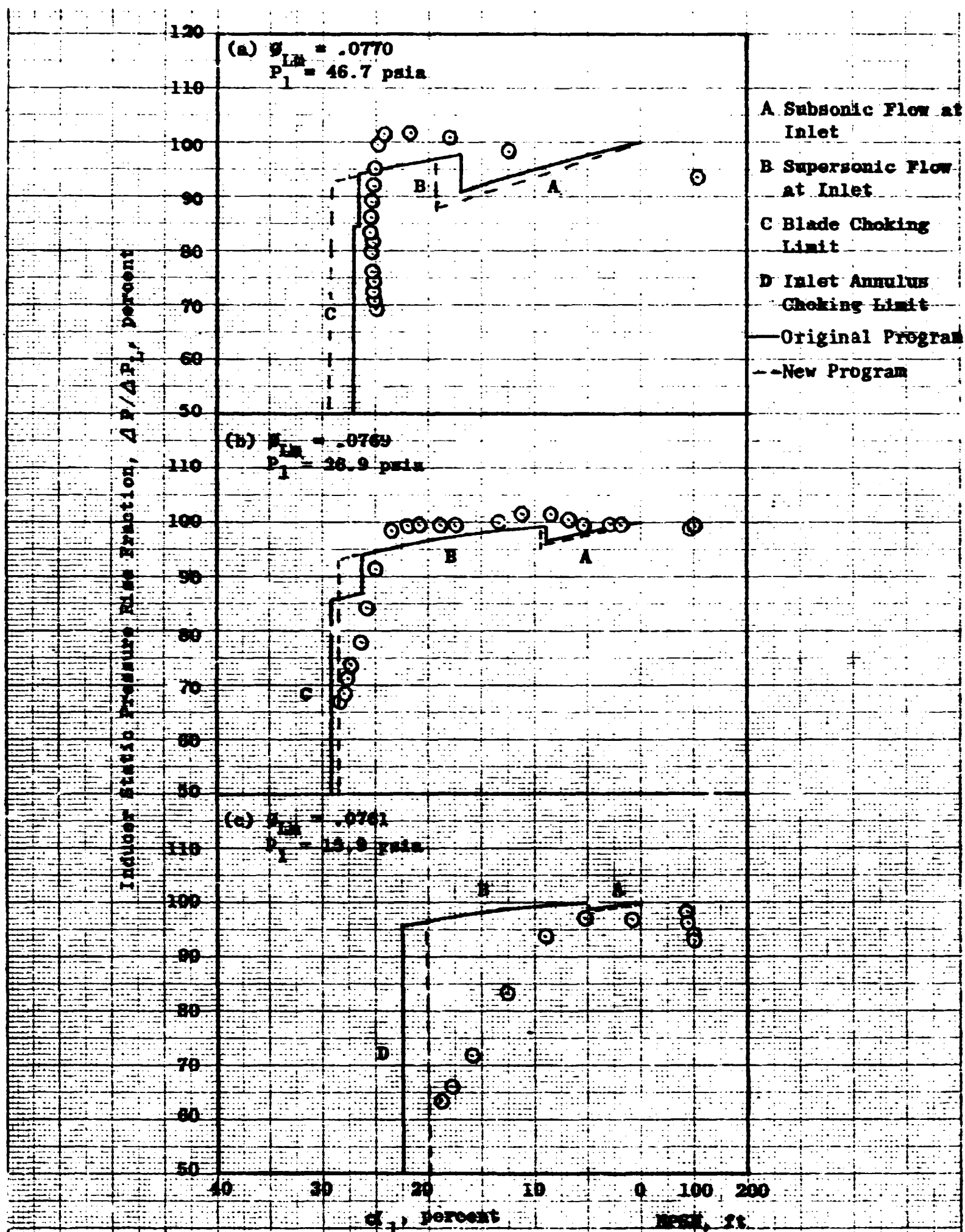


Figure 1.. Comparison of Two-Phase Inducer Flow Model Performance with Two-Phase Hydrogen Inducer Test Performance at 28,000 rpm

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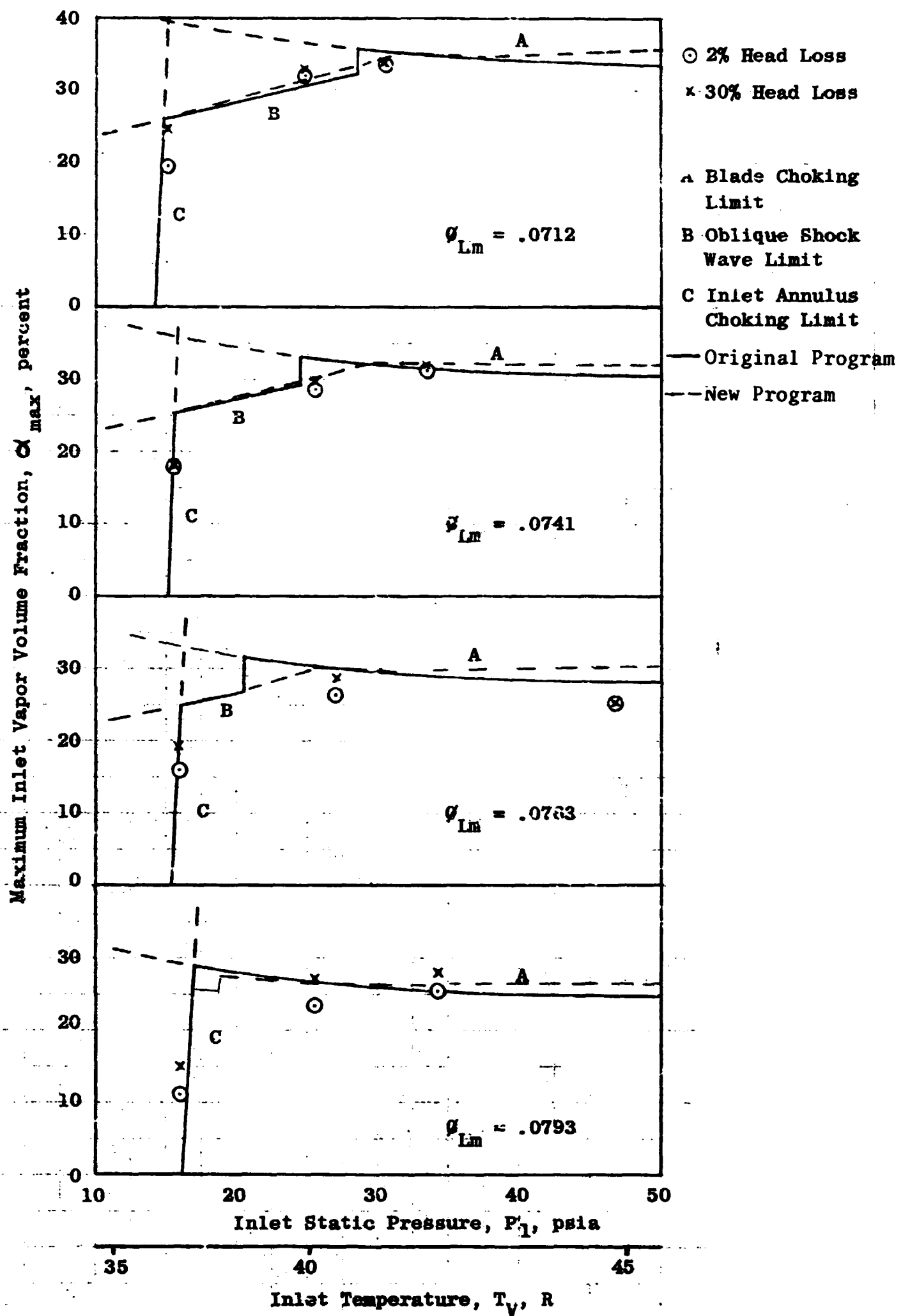


Figure 14. Comparison of Flow Model Vapor Fraction Limits with Test Limits at 28,000 rpm

## INDUCER SELECTION

The two-phase oxygen inducer analysis showed that the criteria for the design of an oxygen inducer for two-phase operation were much the same as for a hydrogen inducer, i.e., a large inlet annulus area to keep the inlet velocity at a minimum and a relatively high incidence-to-blade angle ratio ( $i/\beta$ ). It was found that the inducer that converts the J-2 LOX pump into the J-2S version (the Mark 29-0) met these requirements quite well. A layout with this inducer mounted on a J-2S pump is shown in Fig. 15. The two-phase hydrogen inducer (Ref. 1) had been designed by increasing the inlet diameter from 7.80 to 8.33 inches, an increase of 6.8 percent. The J-2 oxygen inducer had been increased in diameter from 6.75 to 7.25 inches to make the J-2S inducer; this is an increase of 7.4 percent. It can be seen from Fig. 15 that the outer wall of the inducer converges after the throat section. Thus, the approach taken in each inducer is the same.

So that there is no danger of metal rubbing on metal, an unshrouded LOX inducer should have an outer wall made of Kel-F. Kel-F walls and liners have cracked and shattered under the impact of collapsing cavities at high suction specific speeds, so it was highly desirable that the two-phase oxygen inducer be fully shrouded with a Kel-F seal. As can be seen from the layout, the J-2S inducer also has this desirable feature.

Three different criteria were examined to see if the Mark 29-0 pump could handle two-phase oxygen. A check was made to see if the J-2S inducer-pump combination could be run at the desired  $i/\beta$  ratios. Figure 16 is a normalized H-Q map of the J-2S pump. Superimposed are corresponding values of  $i/\beta$ . It can be seen that values up to 0.7 could be attained while the slope of the H-Q characteristic was still negative. This value is substantially higher than the maximum ratio (0.559) run on the two-phase hydrogen inducer.



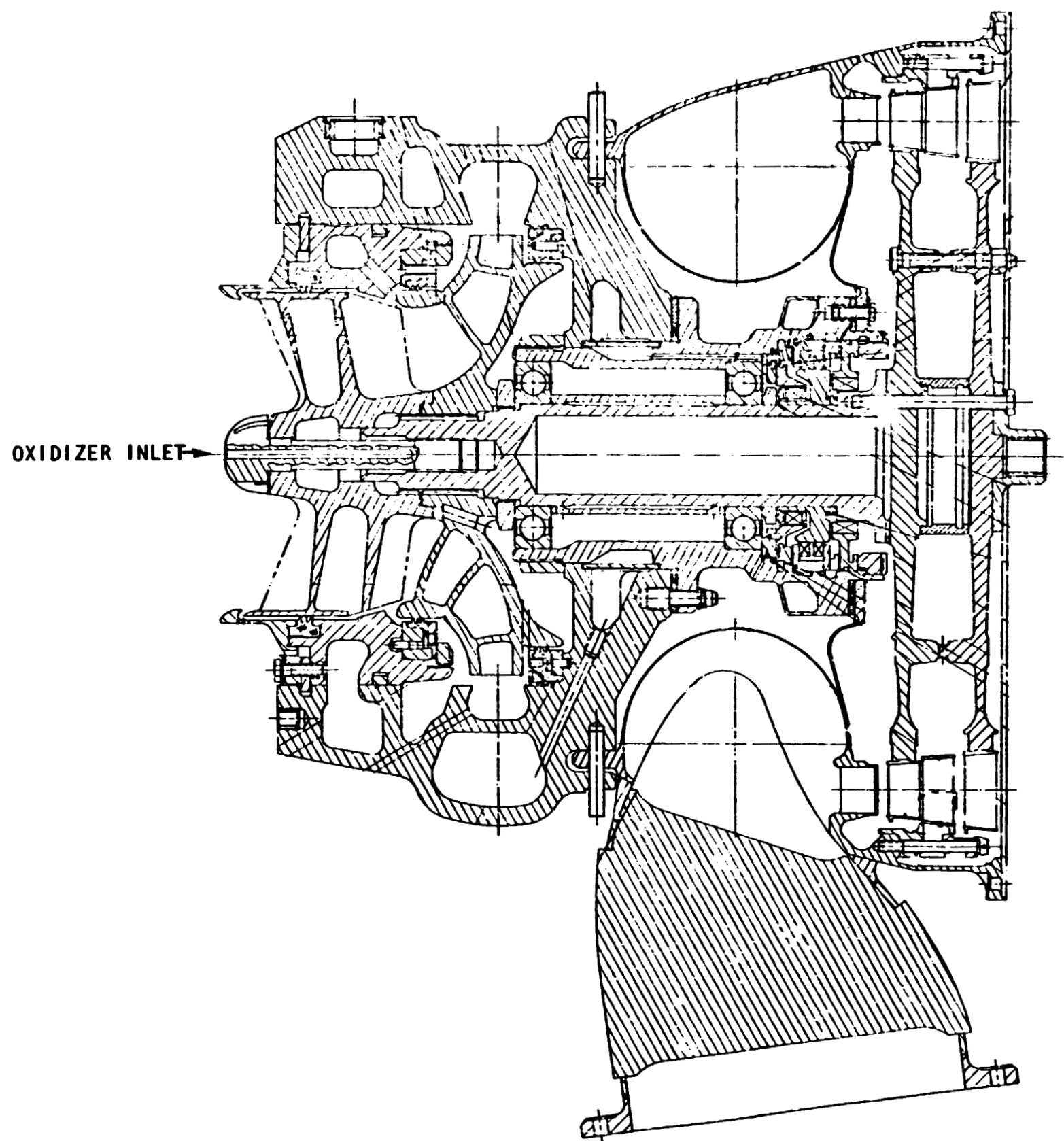


Figure 15. Layout of Mark 29 Oxygen Pump



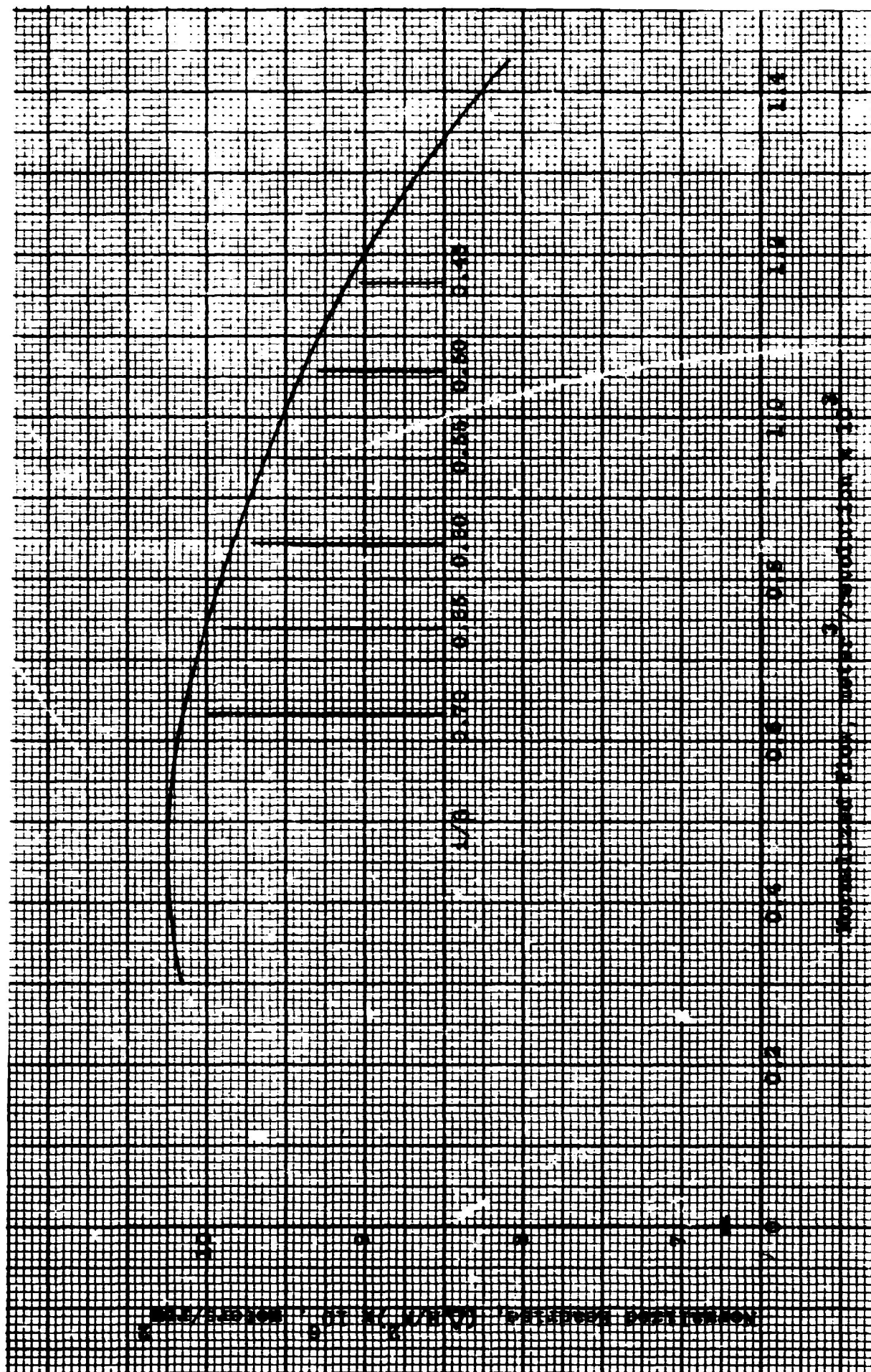


Figure 16. Normalized Headrise vs Normalized Flow of J-2S Oxidizer Pump

A check was also made on the vapor capacity of the J-2, the J-2S and a new two-phase  $O_2$  inducer at the design flow to speed ratio of 0.316. The vapor capacities at the approximate blade choking limit (Ref. 2 and 3) were estimated by using the following equation:

$$\alpha = \frac{1 - \frac{\frac{\phi_{LT}}{1 - B_1} \sqrt{\frac{2}{1 + \lambda^2}}}{\tan \left[ \arcsin \left[ \frac{(1 - B) \sin \beta_m}{1 - \frac{\rho_V}{\rho_L}} \right] \right]}}{1 - \frac{\rho_V}{\rho_L}} \approx 1 - \frac{\phi_{LT} / \tan \beta_T}{(1 - B_1)(1 - B)}$$

The inlet line choking limit was estimated from the predicted equilibrium acoustic velocities (Fig. 1), and the zero tank NPSH requirements were estimated from Fig. 6. The results are shown in Fig. 17 for rotational speeds of 2000, 3000, 4000, and 5000 rpm. The vapor capacities are 16, 25, and 29 percent for the J-2, the J-2S, and a new two-phase  $O_2$  inducer, respectively. At 4000 rpm, the corresponding tank pressures at which saturated liquid oxygen can be pumped are 62, 42, and 26 psia for the J-2, the J-2S, and a new two-phase  $O_2$  inducer, respectively. It was concluded that the difference between 25 and 29 percent vapor capacity was small and that correlatable data and design criteria could be established with the existing J-2S inducer with 25-percent vapor capacity.

The third check was made on the thermodynamic suppression head. If the thermodynamic suppression head (TSH) is insufficient, the flow entering an inducer must be pressurized above the vapor pressure of the liquid (subcooled) to avoid cavitation and subsequent loss in discharge pressure. With insufficient TSH, the two-phase flow relationships (Ref. 1) do not apply because the cavitation

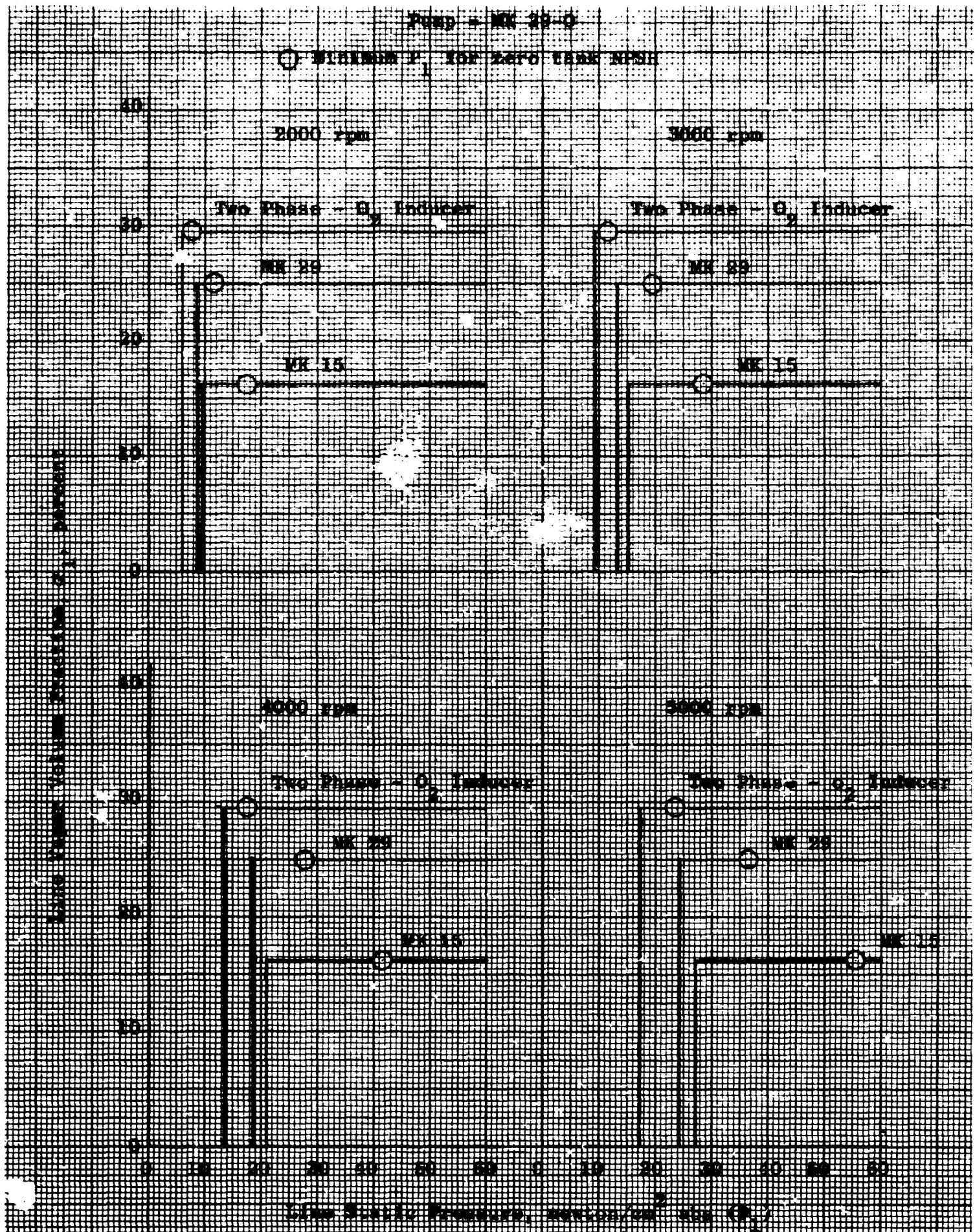


Figure 17. Comparison of Oxygen Inducer Vapor Pumping Limits at Design Q/N

limit occurs before the two-phase inlet condition is reached. Consequently, an analysis of TSH was made to predict the NPSH requirements in oxygen. The results indicated that the TSH was sufficient to permit two-phase operation whenever the inlet line was unchoked. For example, referring to Fig. 18, the Mark 29-0 inducer operating at 4000 rpm and design flowrate has sufficient TSH to operate with saturated liquid oxygen at its inlet if the vapor pressure is above 8 psia. However, because of the inlet line choking limit (which is part of the math model), two-phase pumping will be possible only when the vapor pressure is above 27 psia. As shown, this same relationship between the two limits also applies at other rotational speeds. Thus, the TSH in oxygen was found to be sufficient and the two-phase math model will be applicable over the entire range of test conditions of inlet pressures (15 to 45 psia) without the constraint of adequate TSH.

Because the three criteria were satisfied, it was decided to use the J-2S (Mark 29-0) oxygen pump for the two-phase oxygen tests, and approval was received from the NASA Project Manager. A photograph of the inducer is shown in Fig. 19.



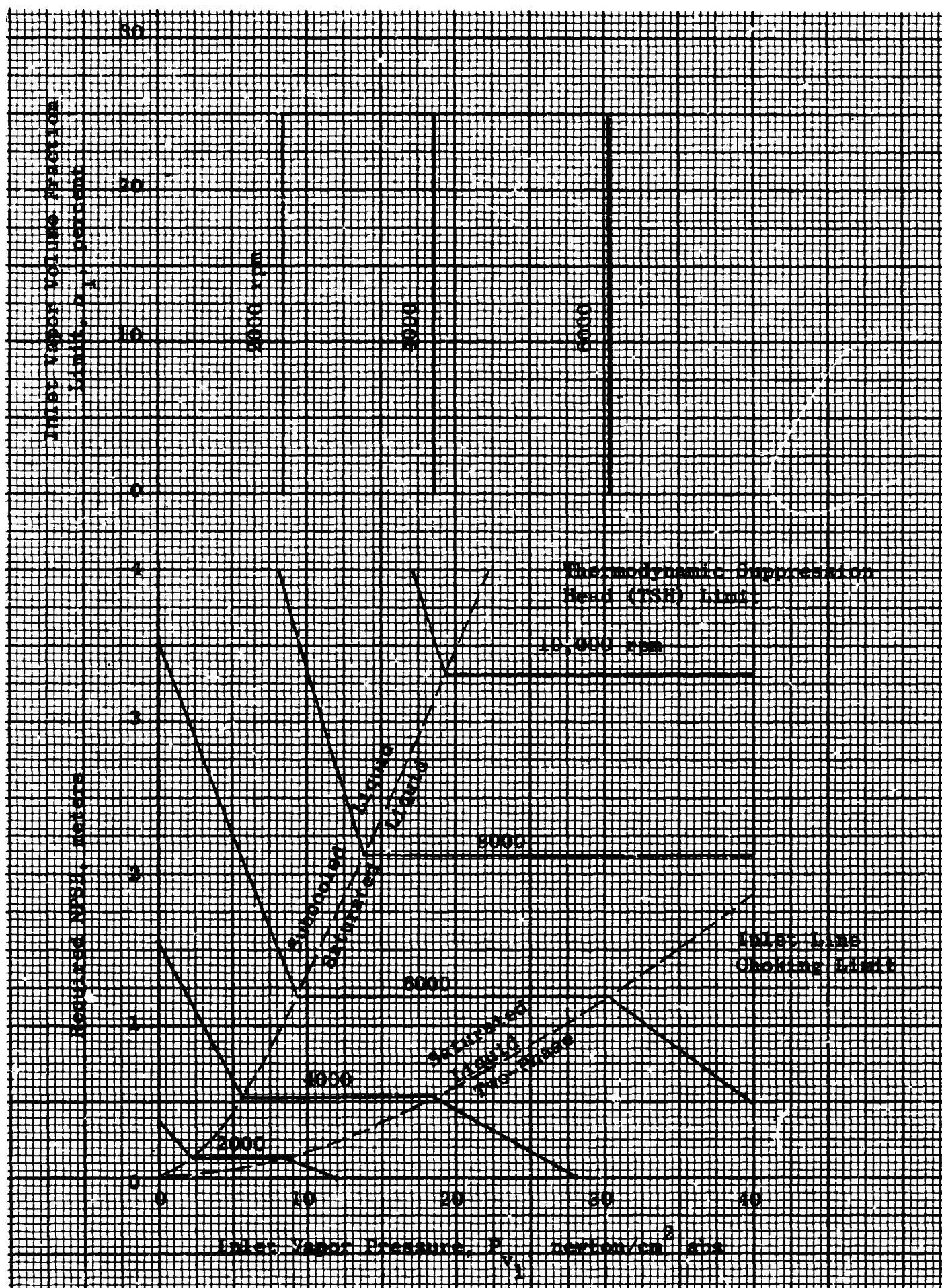


Figure 18. Vapor Fraction Capabilities and NPSH Requirements for the MK 29 Oxygen Inducer at Design Flow Coefficient



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Figure 19. J-2S (Mark 29-0) Inducer

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## TWO-PHASE OXYGEN TESTS

### TEST PLAN

The test results of the two-phase hydrogen inducer had demonstrated the importance of the inducer incidence-to-blade-angle ratio ( $i/\beta$ ) under actual two-phase conditions. For the two-phase oxygen tests, therefore, it was planned to survey this parameter over a wide range. First, the following table was calculated for the inducer which has a 9.10-degree blade angle at the tip.

| $i/\beta$ | Incidence Angle<br>$i = 9.10 \times i/\beta$ | Fluid Angle<br>$\beta_f = 9.1 - i$ | $\phi = C_m/U = \tan \beta_f$ |
|-----------|----------------------------------------------|------------------------------------|-------------------------------|
| 0.2       | 1.82                                         | 7.28                               | 0.12775                       |
| 0.3       | 2.73                                         | 6.37                               | 0.11164                       |
| 0.4       | 3.64                                         | 5.46                               | 0.09558                       |
| 0.5       | 4.55                                         | 4.55                               | 0.07958                       |
| 0.6       | 5.46                                         | 3.64                               | 0.06362                       |
| 0.7       | 6.37                                         | 2.73                               | 0.04768                       |
| 0.8       | 7.28                                         | 1.82                               | 0.03178                       |

From past experience it was known that oxygen tests could be run very rapidly if the run tank contained saturated liquid oxygen at atmospheric pressure. This is equivalent to a temperature of about 162 R. It was decided, therefore, that the maximum amount of data could be obtained at these conditions. Figure 20 shows an expansion curve of saturated oxygen at 162 R. Note that at a typical vapor fraction of 30 percent, the fluid velocity is about 6 ft/sec. At the mean  $i/\beta$  of 0.5, the corresponding inducer tip speed is  $6 \div 0.07958 = 75$  ft/sec.

An H-Q plot of the J-2S (Fig. 21) was made at inducer tip speeds in the vicinity of 75 ft/sec. At each tip speed the fluid velocities corresponding to  $i/\beta$  values of 0.2 to 0.8 were calculated, and from the curve on Fig. 20 these were used to determine  $\dot{W}/A$ . These were, in turn, converted to gallons per minute and spotted on Fig. 21. From this matrix the test plan in terms of Q and N was formulated.

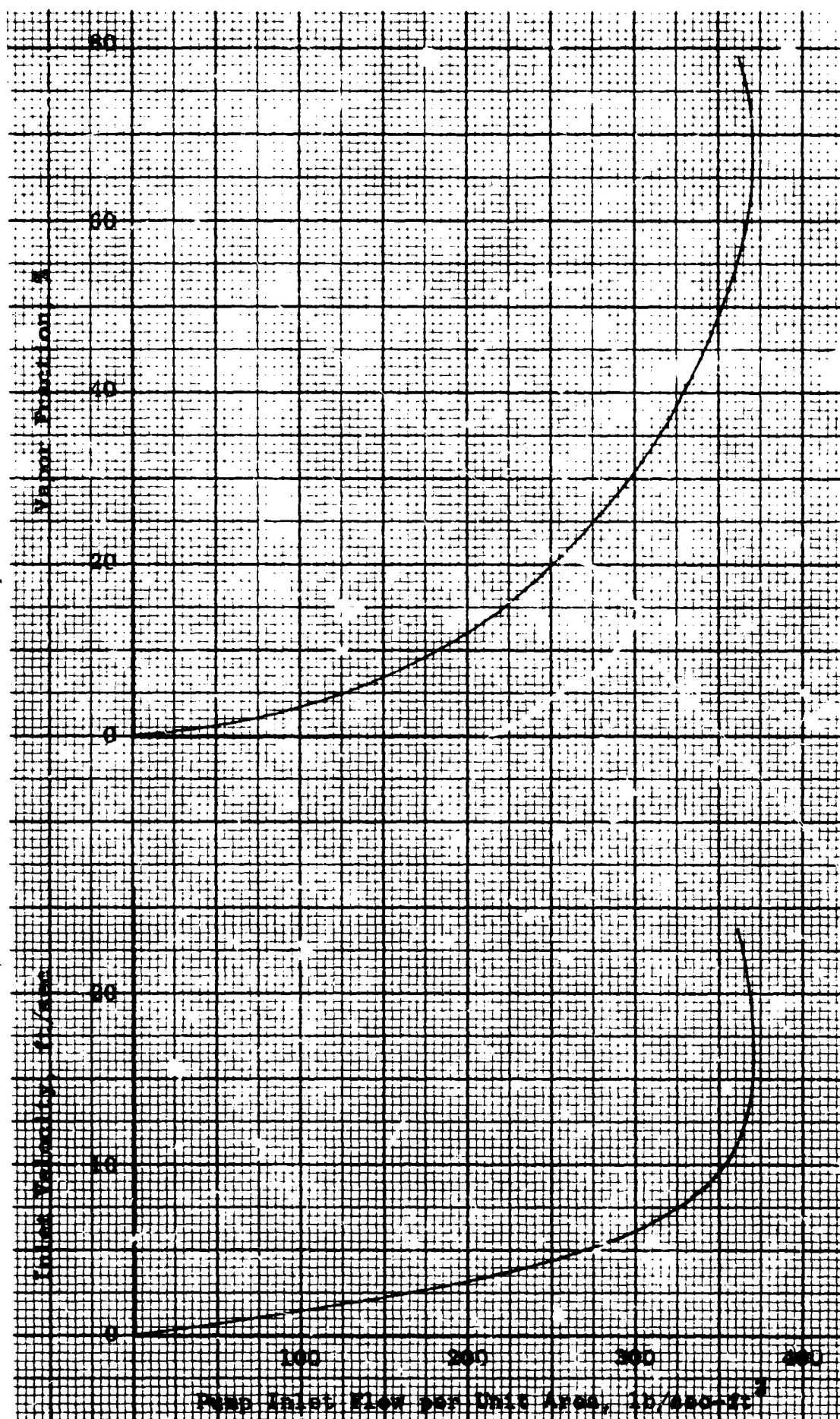


Figure 20. Oxygen Expanded From a Saturated Temperature of 162 R

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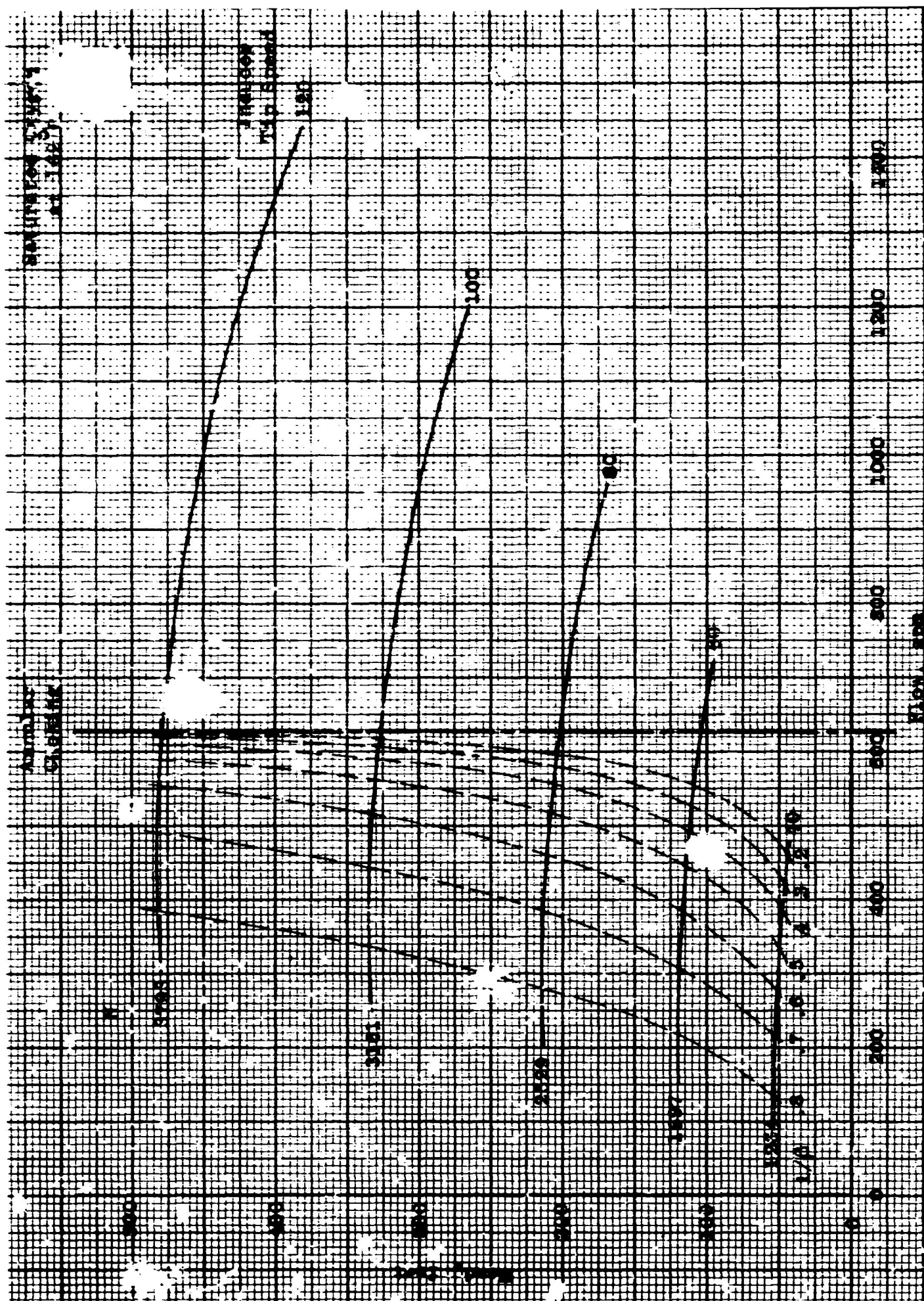


Figure 21. J-2 H-Q Map and Saturated Oxygen Limits

## TEST FACILITY AND INSTRUMENTATION

The test program was conducted at Component Test Laboratory 1 (CTL-1), Cell 2A, Santa Susana Field Laboratory. The pumped fluid was liquid oxygen for all tests.

A simplified schematic of the facility is shown in Fig. 22. The run tank is elevated above the test pump position. The flow mode is from run tank to catch tank, and the propellant is transferred back to the run tank under gravity feed between runs.

The configuration of the inlet ducting is shown in Fig. 23. The straight horizontal run to the pump is approximately 118 inches long. The ducting reduces from 10 to 7 inches starting 58 inches from the pump inlet. A throttling section was incorporated at the flange interface between the reducer and the 7-inch spool. The throttling section consisted of multiple layers of fine mesh wire screens (40, 200, and 400 mesh) backed by a Frantz-type screen (approximately 5/32-inch triangular openings) for structural support.

The significant pump instrumentation is also shown in Fig. 23. Static wall pressure measurements are indicated by PS1, PS4, PD1, and PD2. Resistance temperature measurements are indicated by TS1 through TS4, TD1, and TD2. Dynamic pressure measurements (Photocons) were made at stations indicated by PH1 through PH3. A complete listing of instrumentation is contained in Appendix B.

Pump flow was measured with a turbine-type flowmeter in the discharge line. Flow modulation was accomplished with a hydraulically actuated throttle valve which was servocontrolled off the flowmeter signal.

Bulk fluid temperature conditioning was accomplished by venting to atmosphere to obtain the -298 F (90 K) condition, and by pressurizing and holding for the higher temperatures of -288 F (95.5 K) and -278 F (101 K). (NOTE: The run tank and all feed lines were uninsulated.)

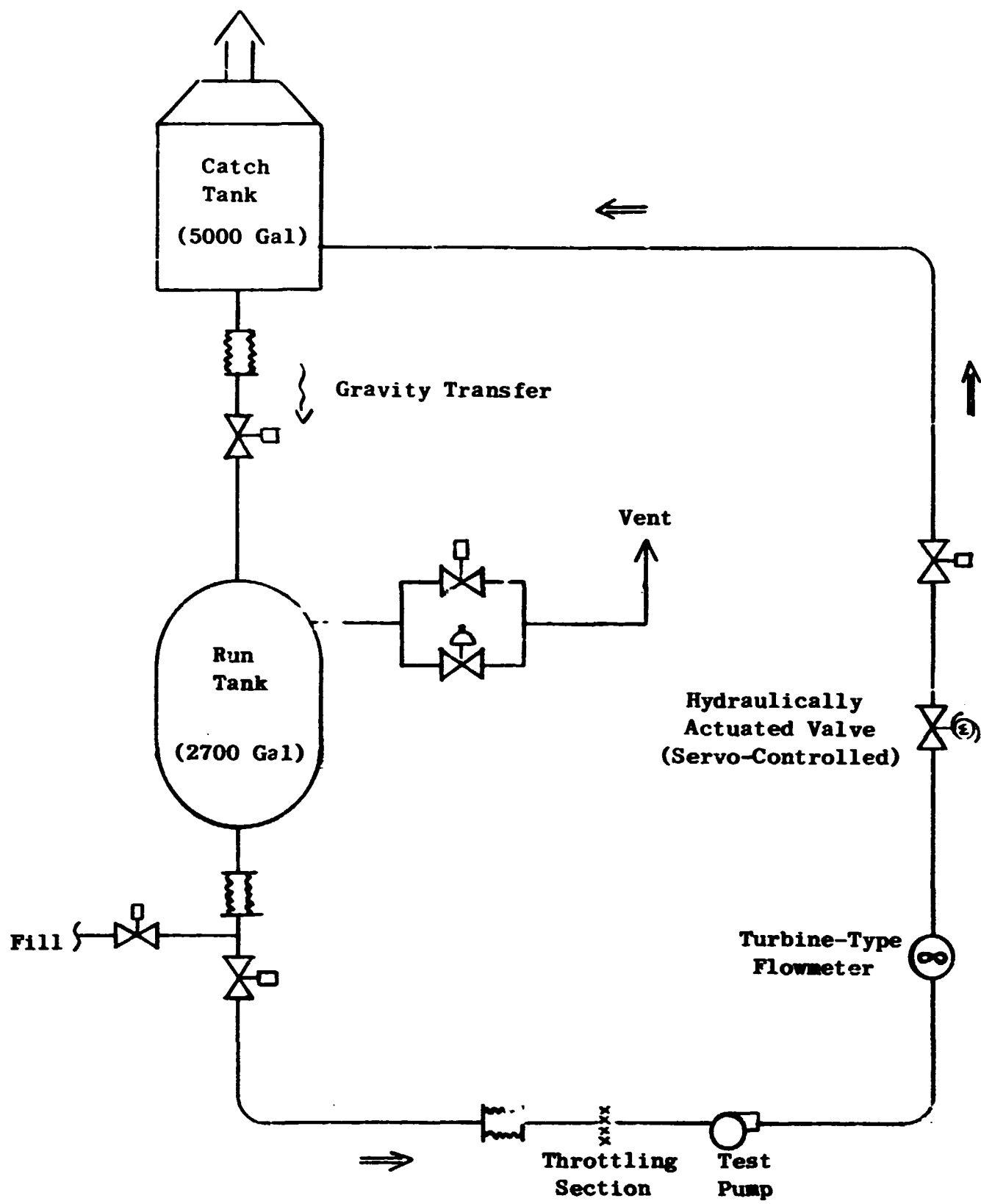


Figure 22. Schematic of CTL 1, Cell 2A Pump Test Facility

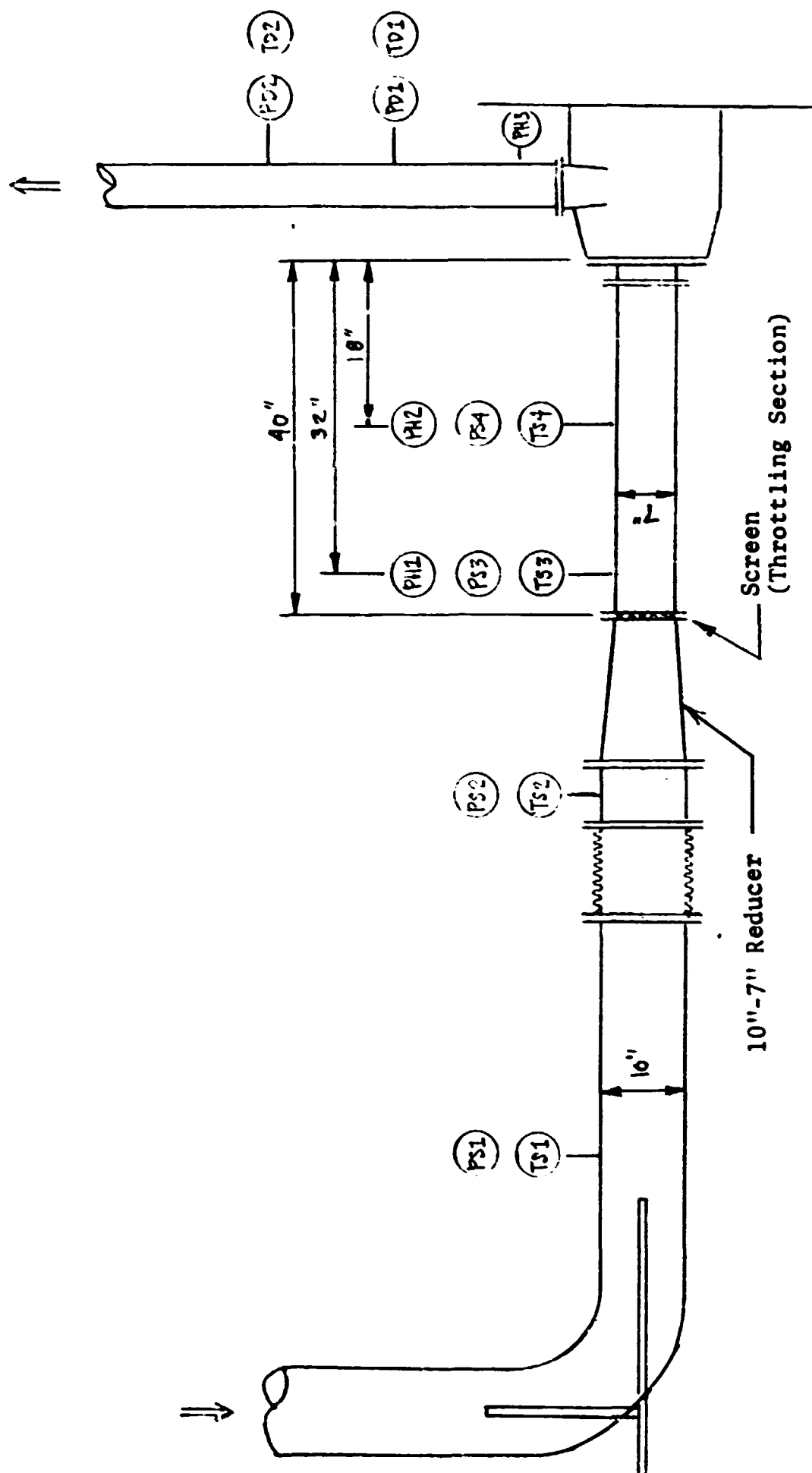


Figure 23. CTL 1, Cell 2A Two-Phase LOX Pump Testing- Inlet Ducting and Pressure Tap Locations

The bulk of the testing was done at a LOX fluid temperature of -298 F (90 K). Wide flow range data were obtained at targeted speeds of 1897 and 2529 rpm. In addition, several flow points were obtained at higher speeds of 3161 and 3793 rpm.

Testing at the higher temperatures was limited by the time-consuming fluid-conditioning process. Two tests each at -288 F (95.5 K) and -278 F (101 K) were conducted to investigate temperature effects.

The complete test series consisted of an overall total of 47 tests. Satisfactory cavitation data were obtained on 29 tests. The detailed test summary is shown in Appendix C.

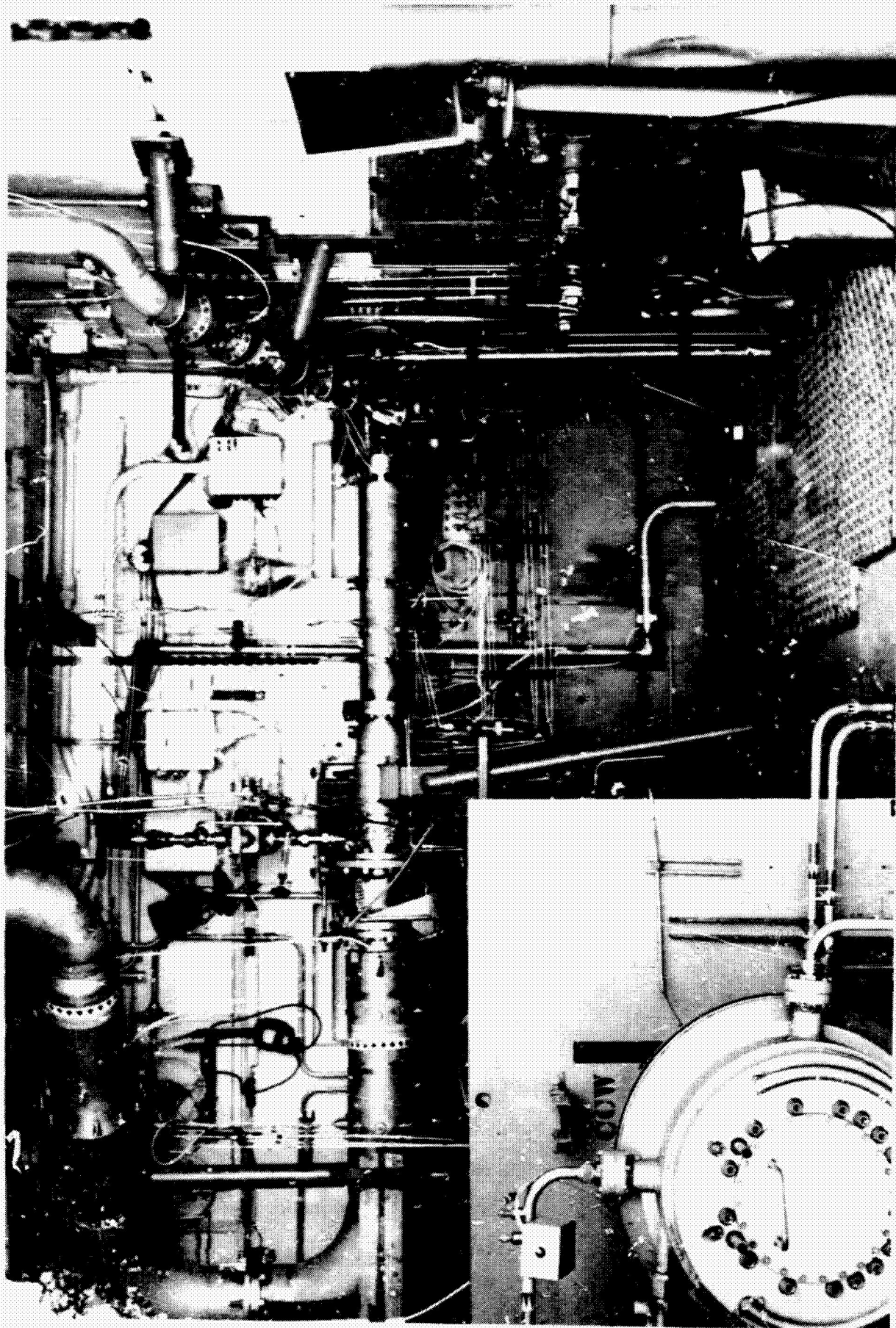
Figure 24 depicts the overall test installation. The inlet ducting breaks through from the upper deck and makes the horizontal run from left to right. The test pump is mounted to a jib plate on the right wall. Pump discharge is inclined upward.

The pump drive consists of three 1000-horsepower d-c motors in tandem, a 15,000-horsepower gear box, and a 3000-horsepower T-gear box.

#### TEST PROGRAM

The test series consisted of constant speed cavitation runs. For most tests the flow was held constant within the limitations of the servo-controlled throttle valve. In some cases the throttle valve was held in a constant position due to flow controller problems and the flow was permitted to drop off as the inlet pressure was vented.

In order to obtain two-phase conditions at the pump inlet it was necessary to obtain a sufficient pressure drop across the throttling section to overcome an elevation head of approximately 8 feet with the elevated run tank vented to zero gage pressure. This was accomplished by using various combinations of fine mesh wire cloth backed by a Frantz-type screen.



IYW91-9/23/71-SIF

Figure 24. Test Installation



Due to structural limitations of the Frantz screen (35 psi or 24 newton/cm<sup>2</sup> delta pressure maximum) it was necessary to vary the number of wire cloth laminations depending on the flow range being investigated. Another difficulty experienced was the unpredictability of pressure drop predictions for multiple laminations of fine mesh wire cloth restrained only at the rim. At low flow (or low pressure drop) the laminations tend to be sprung apart and yield a certain characteristic. Increasing the flow (and pressure drop) seems to form a "tighter" screen pack and radically changes the blockage.

The wire cloth used ranged from 40-mesh to 400-mesh with wire gage commensurate for each mesh size to yield the necessary blockage.

#### TEST RESULTS

Test data were recorded and processed by means of a digital data-acquisition system (Astrodata), while supplementary direct-inking graphic recorders (DIGR) permitted visual monitoring of critical parameters during a test.

A computer program was written to process the test data, incorporating Rocketdyne-developed subroutines for thermodynamic properties of oxygen. The basic technique for determining the vapor fraction at the pump inlet was consistent with the test setup and instrumentation, a constant enthalpy throttling process across the screen package in the pump suction line.

$$h_{1s} + \frac{V_1^2}{2gJ} = h_{2s} + \frac{V_2^2}{2gJ}$$

The total enthalpy was determined from the upstream conditions of pressure, temperature, and known mass flowrate. After an initial check to determine whether the downstream conditions may be in a liquid state (in which case the normal NPSH calculations are performed), an iteration process based on density of the two-phase mixture was performed until the total downstream enthalpy agreed with the upstream enthalpy.



An alternate method to calculate the vapor fraction was investigated. This is the momentum method given in Appendix A. However, the results were found to be consistent with that obtained with the enthalpy method within a percentage point in vapor fraction. All test results presented in this report are based on the enthalpy routine.

The basic cavitation curves are shown in Fig. 25 through 53. The data are grouped by fluid temperature level, speed, and flow:

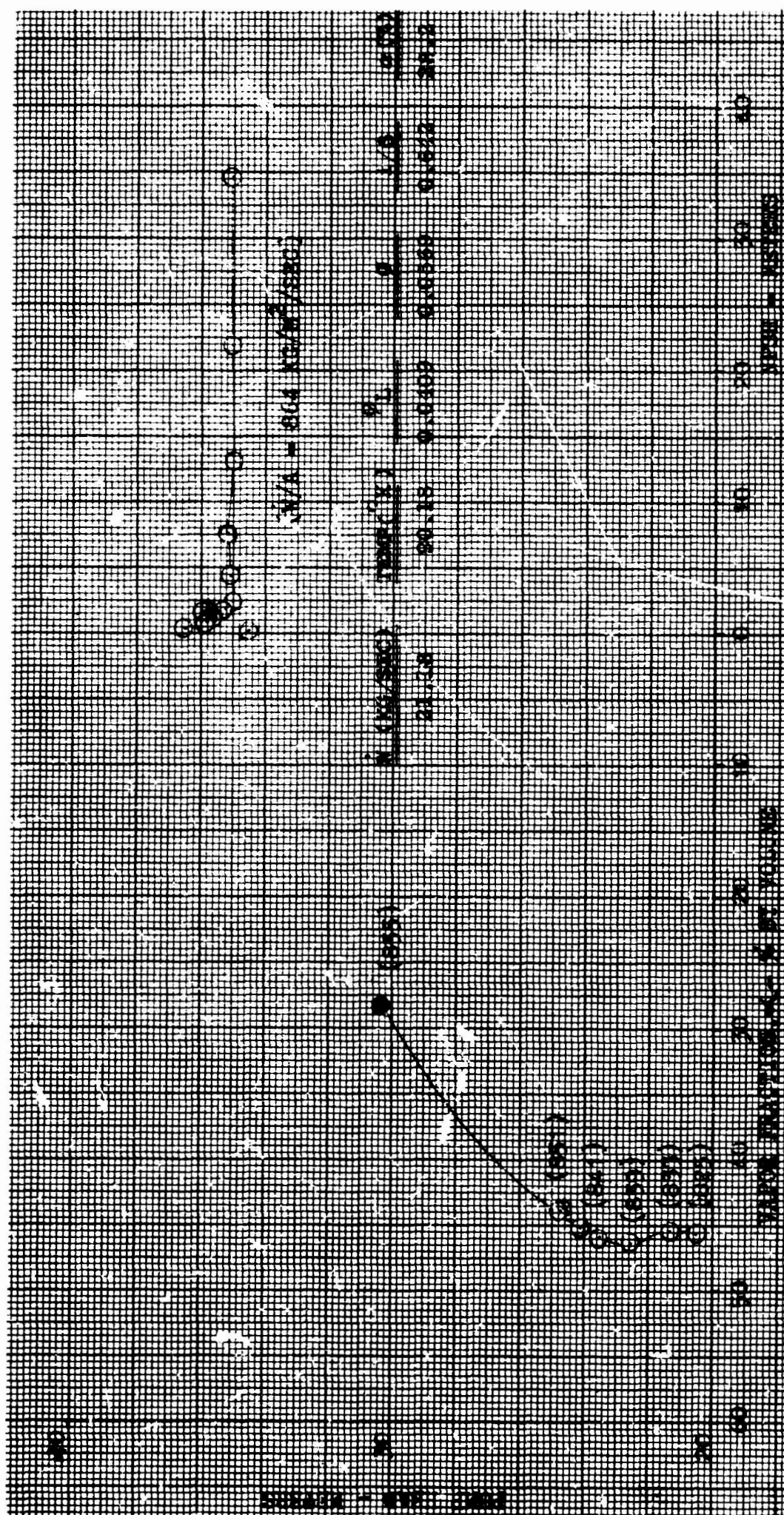
| Figures       | Temperature,<br>K | N (Rad/Sec) |
|---------------|-------------------|-------------|
| 25 through 32 | 90                | 200         |
| 33 through 44 | 90                | 265         |
| 45 through 47 | 90                | 330         |
| 48 and 49     | 90                | 400         |
| 50 and 51     | 95                | 400         |
| 52 through 53 | 102               | 265/410     |

In each case the critical point at the "knee" in the head dropoff characteristic is indicated by the shaded symbol, and the more significant parameters at this condition are tabulated. The parameters include:  $\dot{W}$  (mass flowrate), fluid temperature,  $\phi_L$  (liquid flow coefficient),  $\phi$  (actual flow coefficient),  $i/\beta$  (ratio of incidence to blade angle), and  $\alpha$  (percent vapor by volume).

All test data presented were based on the pump inlet station closest the inducer. There was a distinct difference in vapor content between the two stations downstream of the screen, with the vapor fraction increasing in the downstream direction. It is suspected that readings closest to the screen were reflecting non-equilibrium conditions.

A summary plot of the test results is contained in Fig. 54. The critical point parameters chosen for correlation were  $\phi_L$ ,  $\alpha$ , and  $i/\beta$ .

The liquid flow coefficient ( $\phi_L$ ) is essentially a  $\dot{W}/N$  term, and thus constitutes a suitable independent variable for correlating data obtained at various speeds.







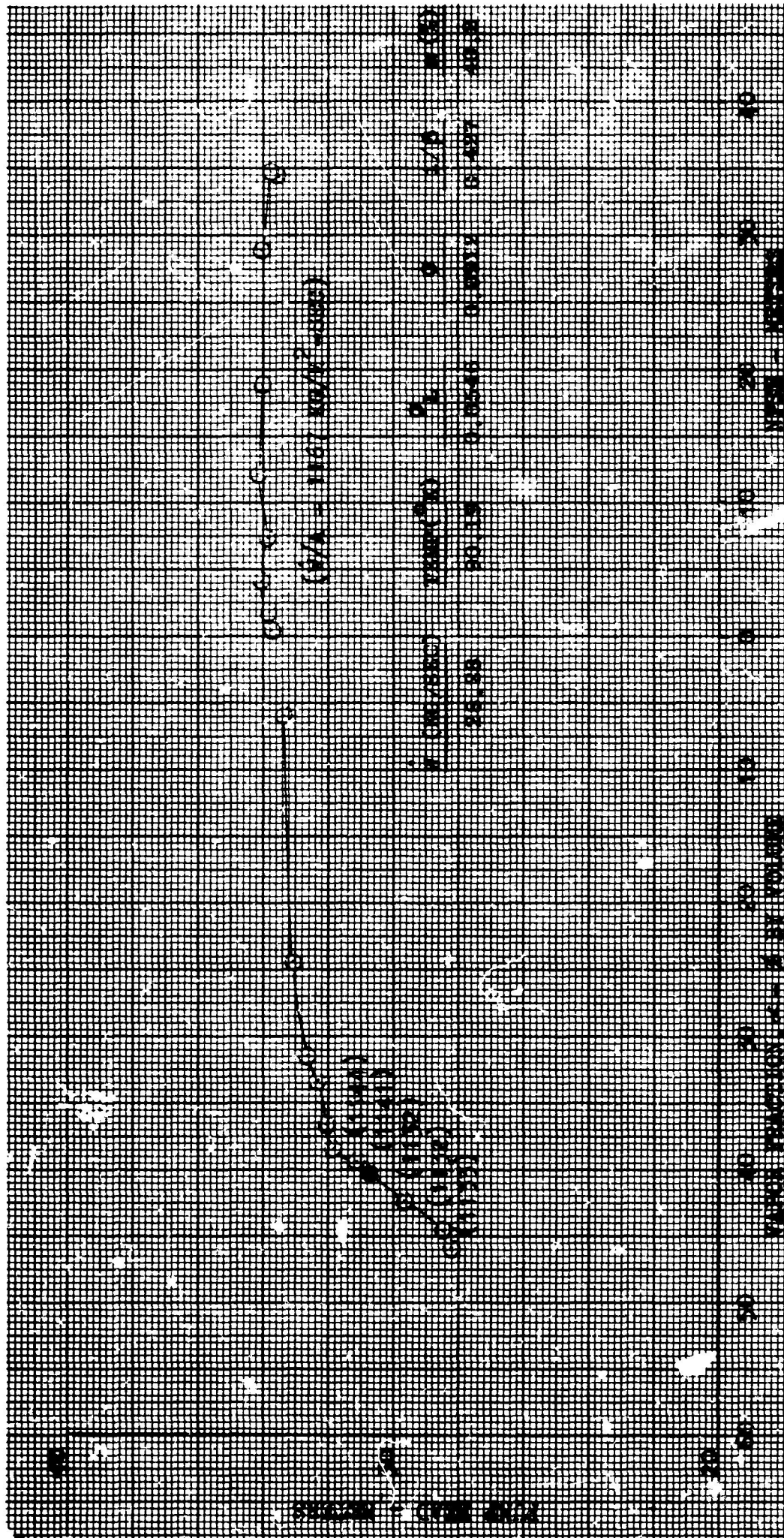


Figure 27. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713017, Test Speed = 197 Rad/Sec

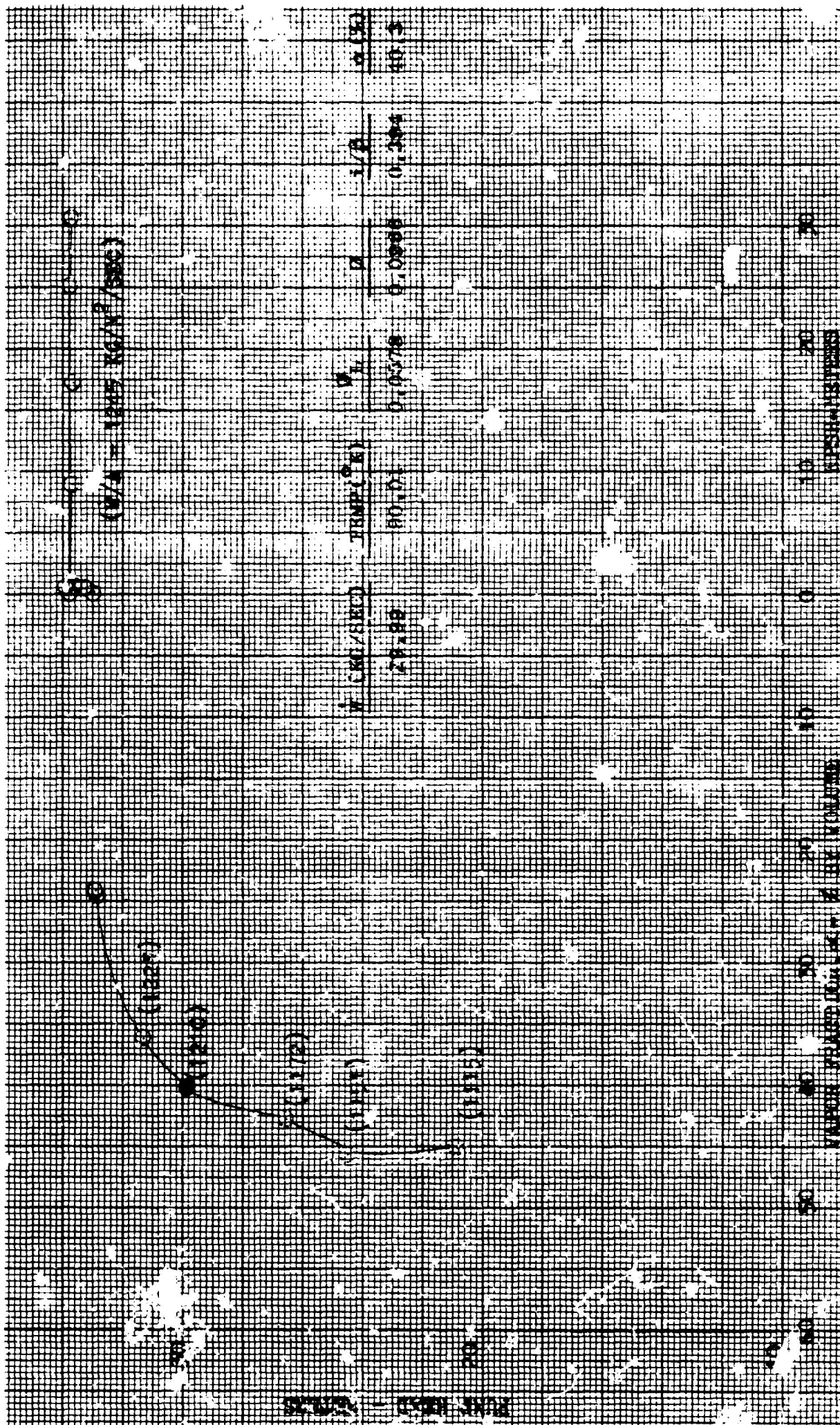


Figure 28. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713027, Test Speed = 197 Ra.1/Sec





Figure 29. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713014, Test Speed = 205 Rad/Sec

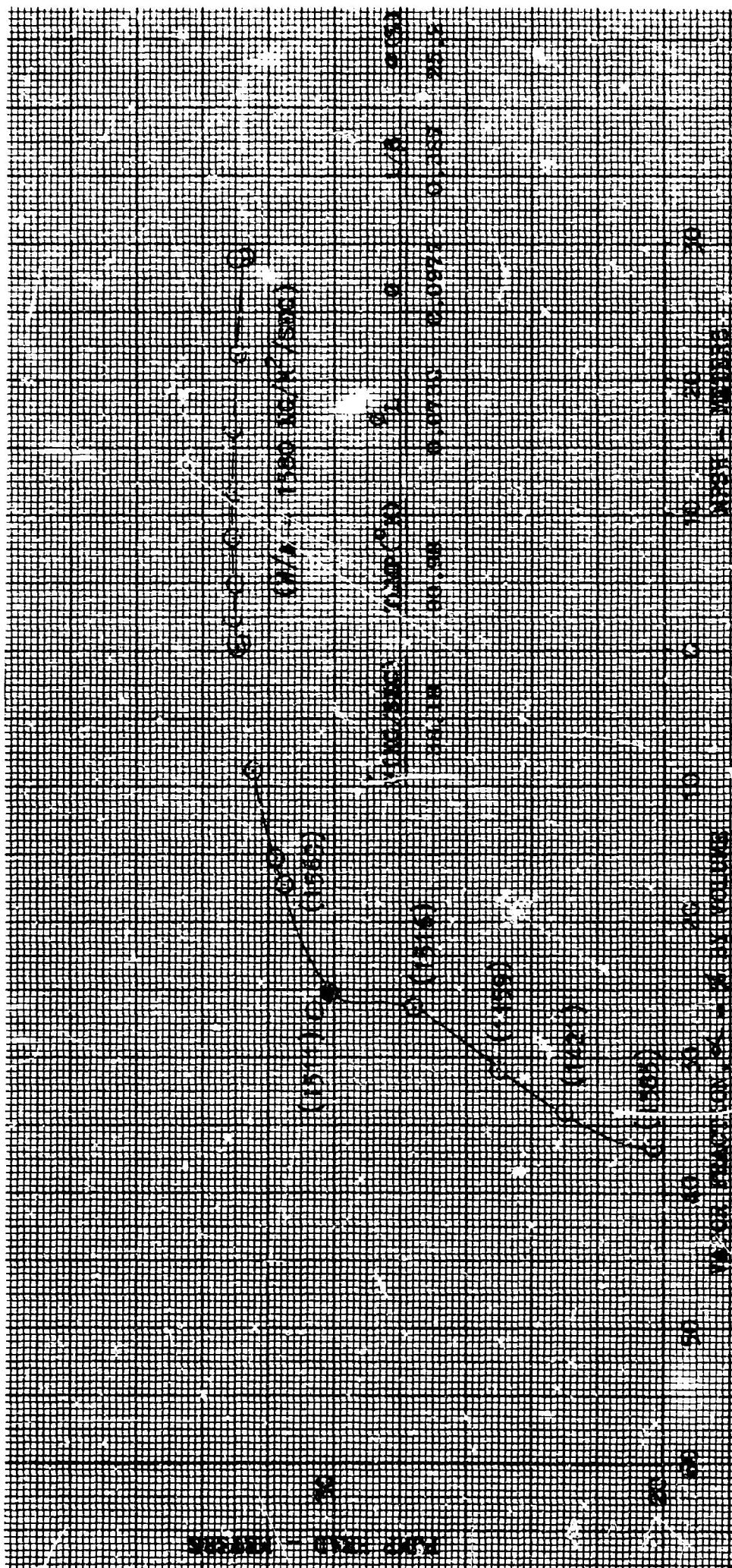


Figure 30. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713026, Test Speed = 200 Rad/Sec



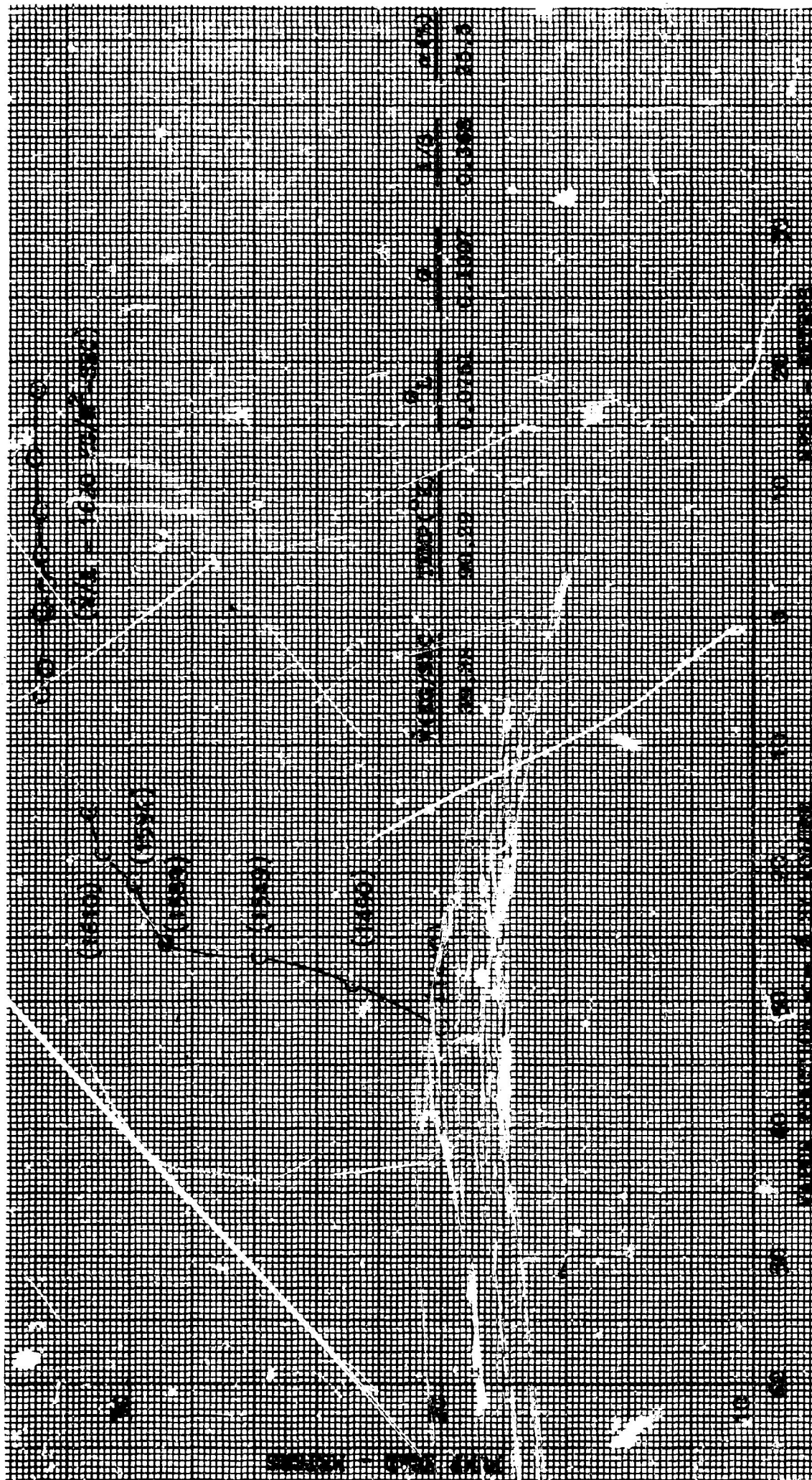


Figure 31. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713013,  
Test Speed = 200 rad/sec

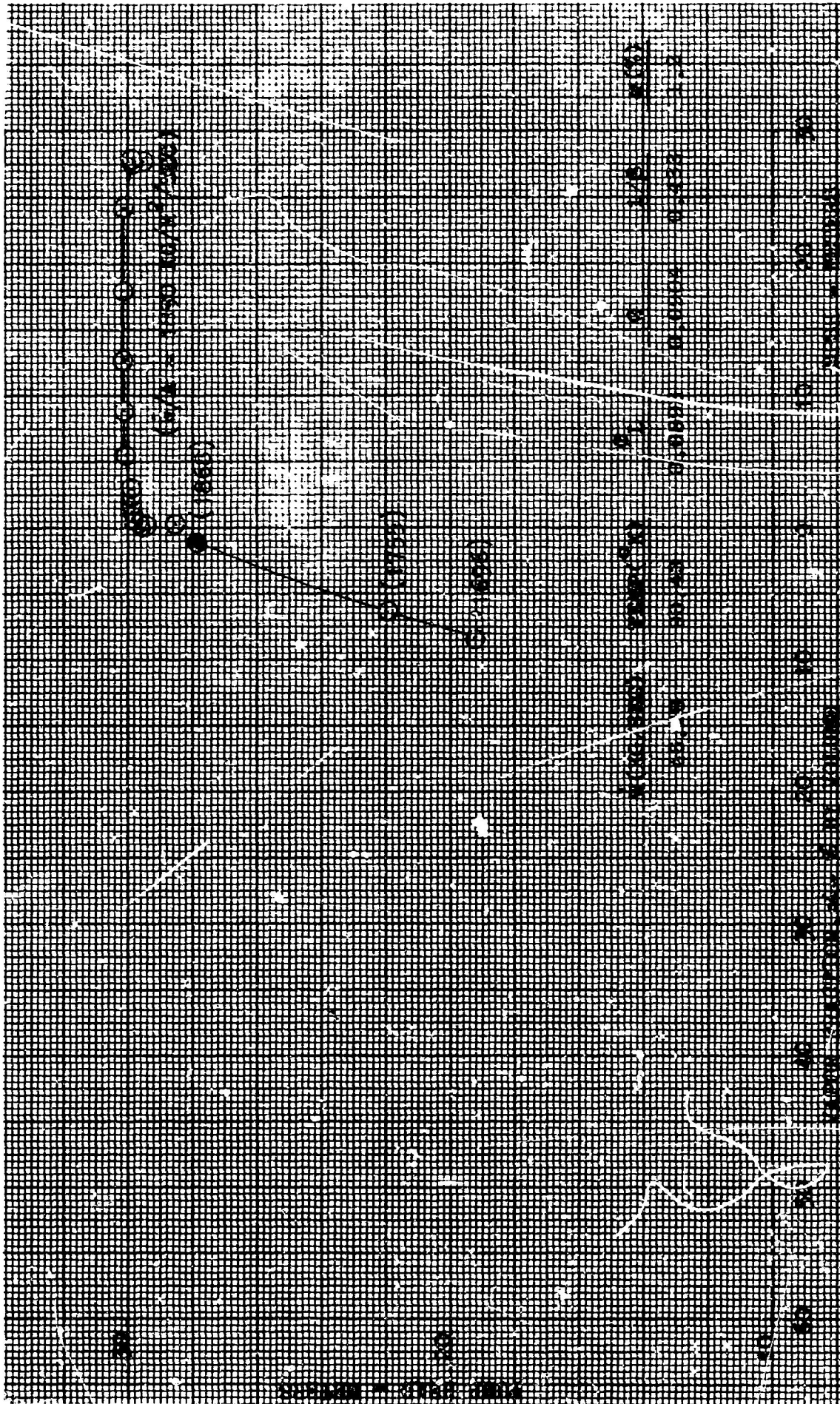


Figure 32. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713041,  
Test Speed = 198 rad/sec



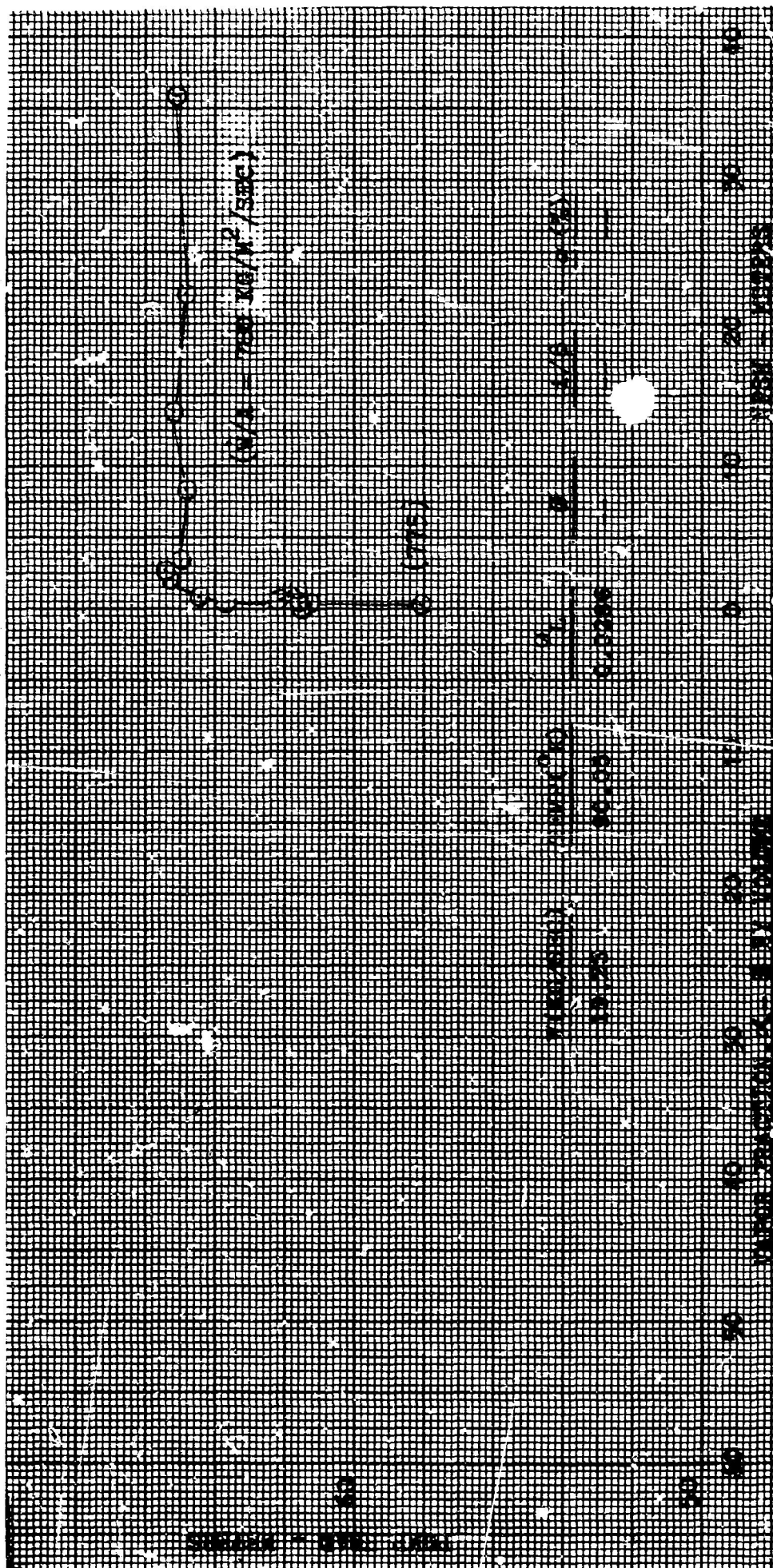
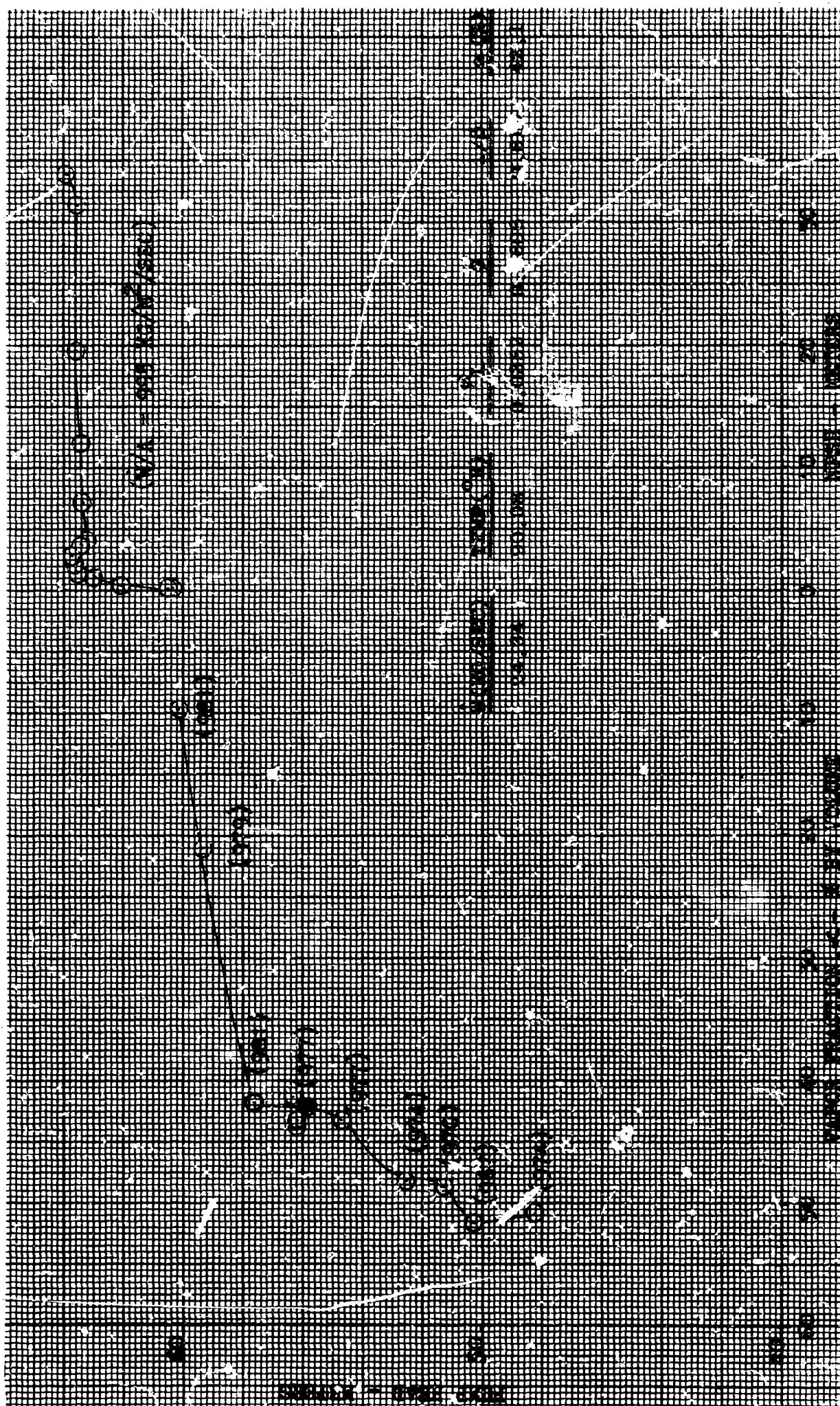


Figure 33. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713044,  
Test Speed = 267 rad/sec





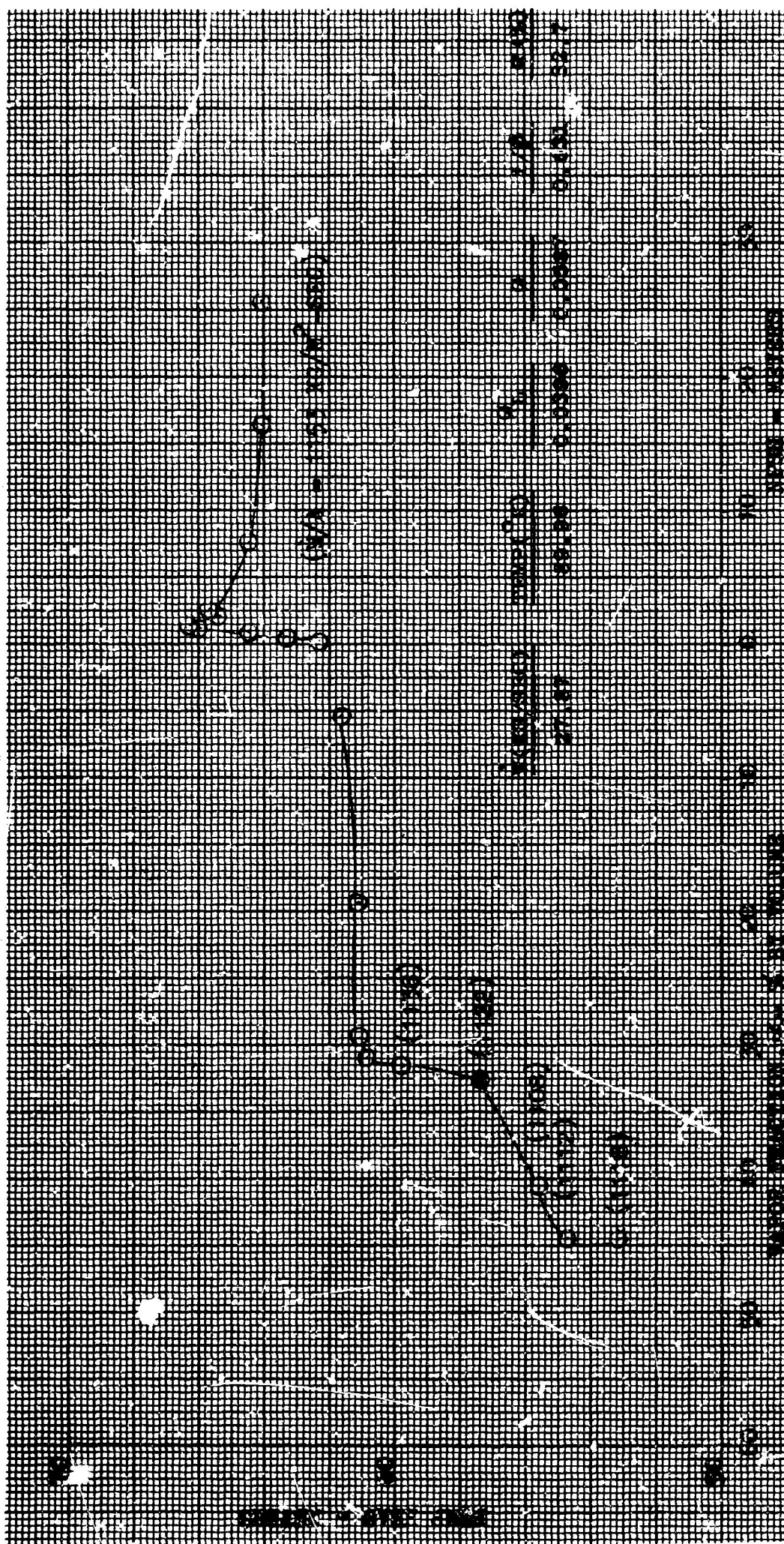


Figure 35. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713023,  
Test Speed = 268 rad/sec

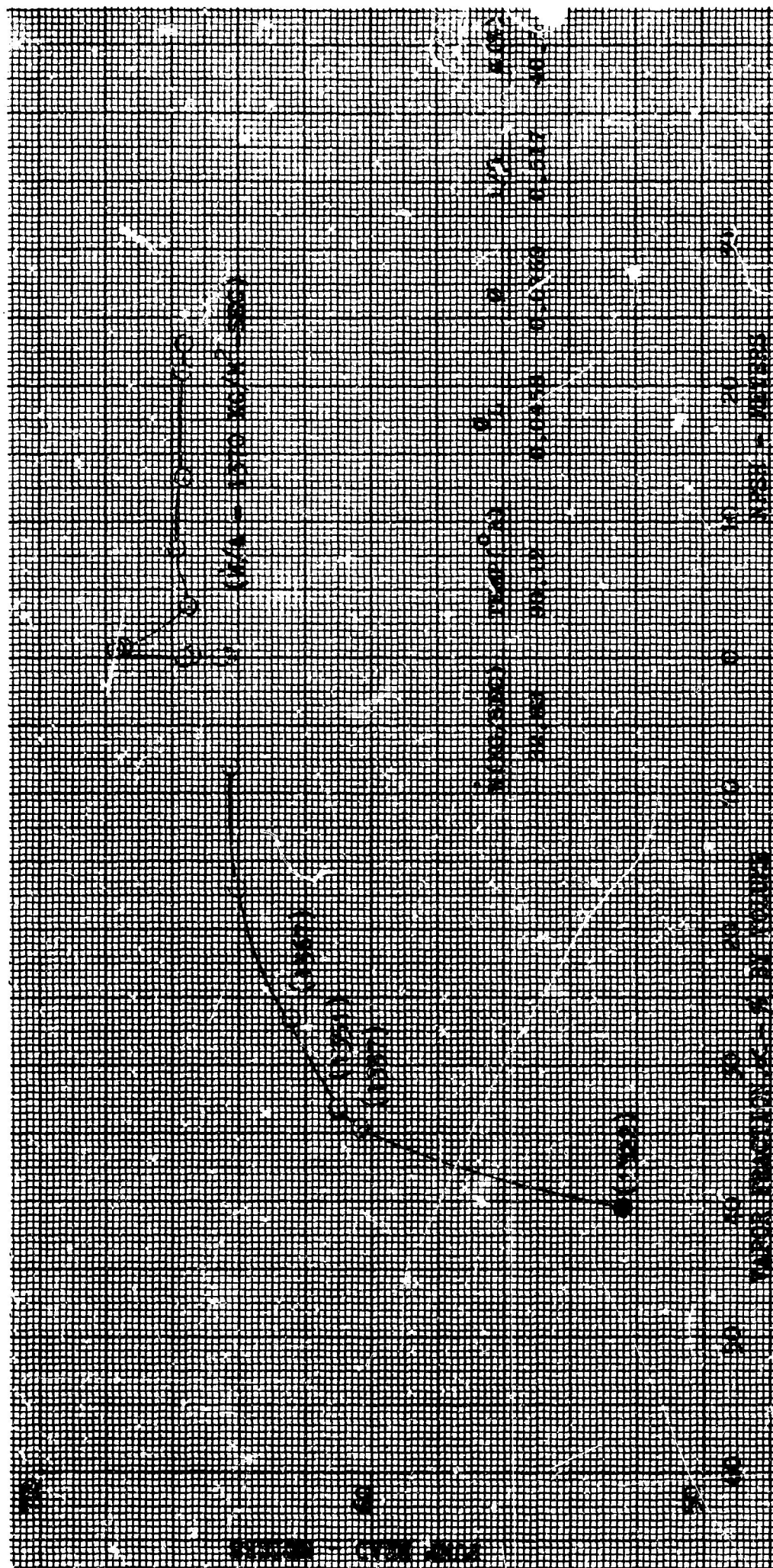
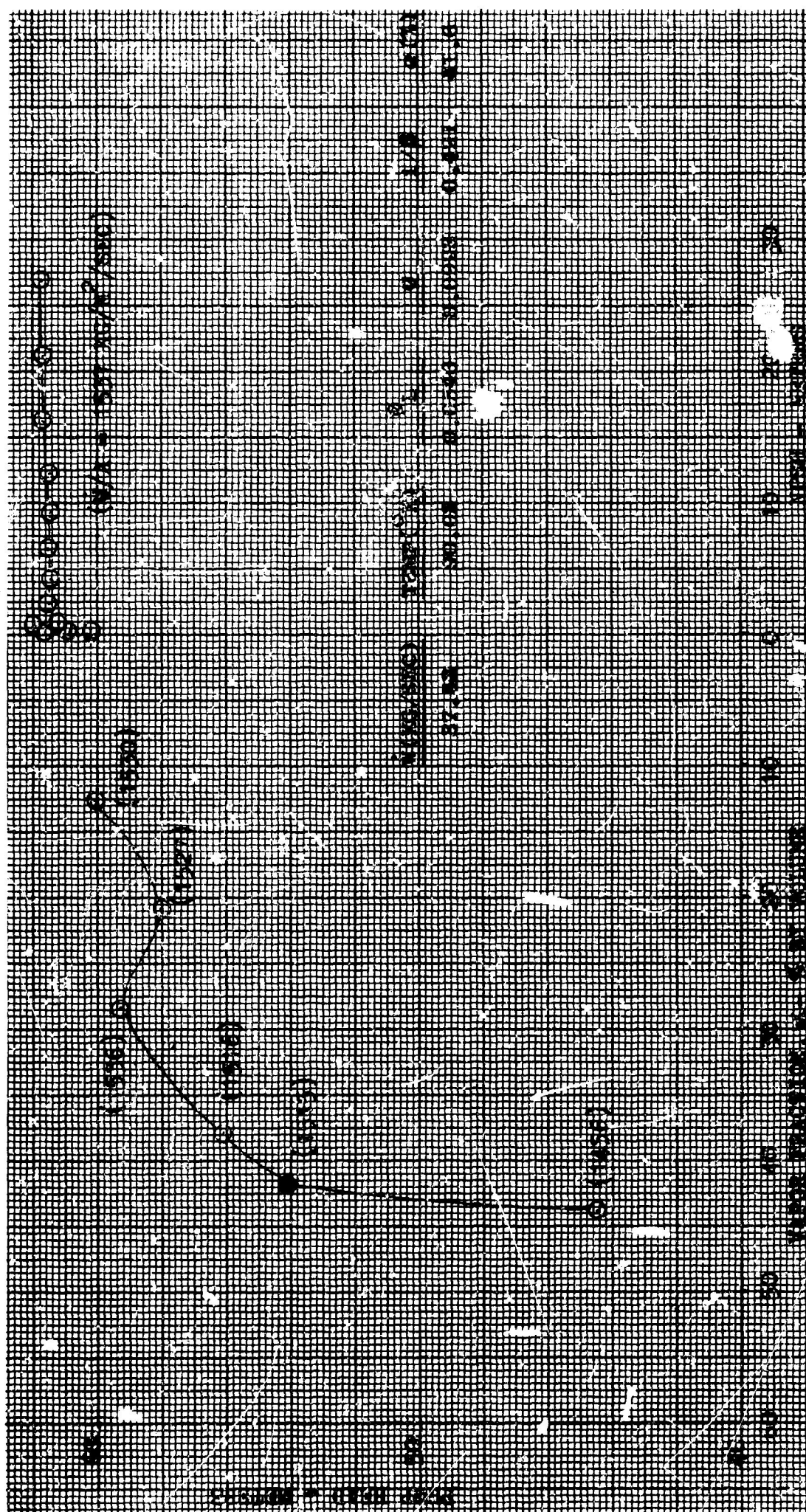


Figure 36. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713022,  
Test Speed = 272 rad/sec

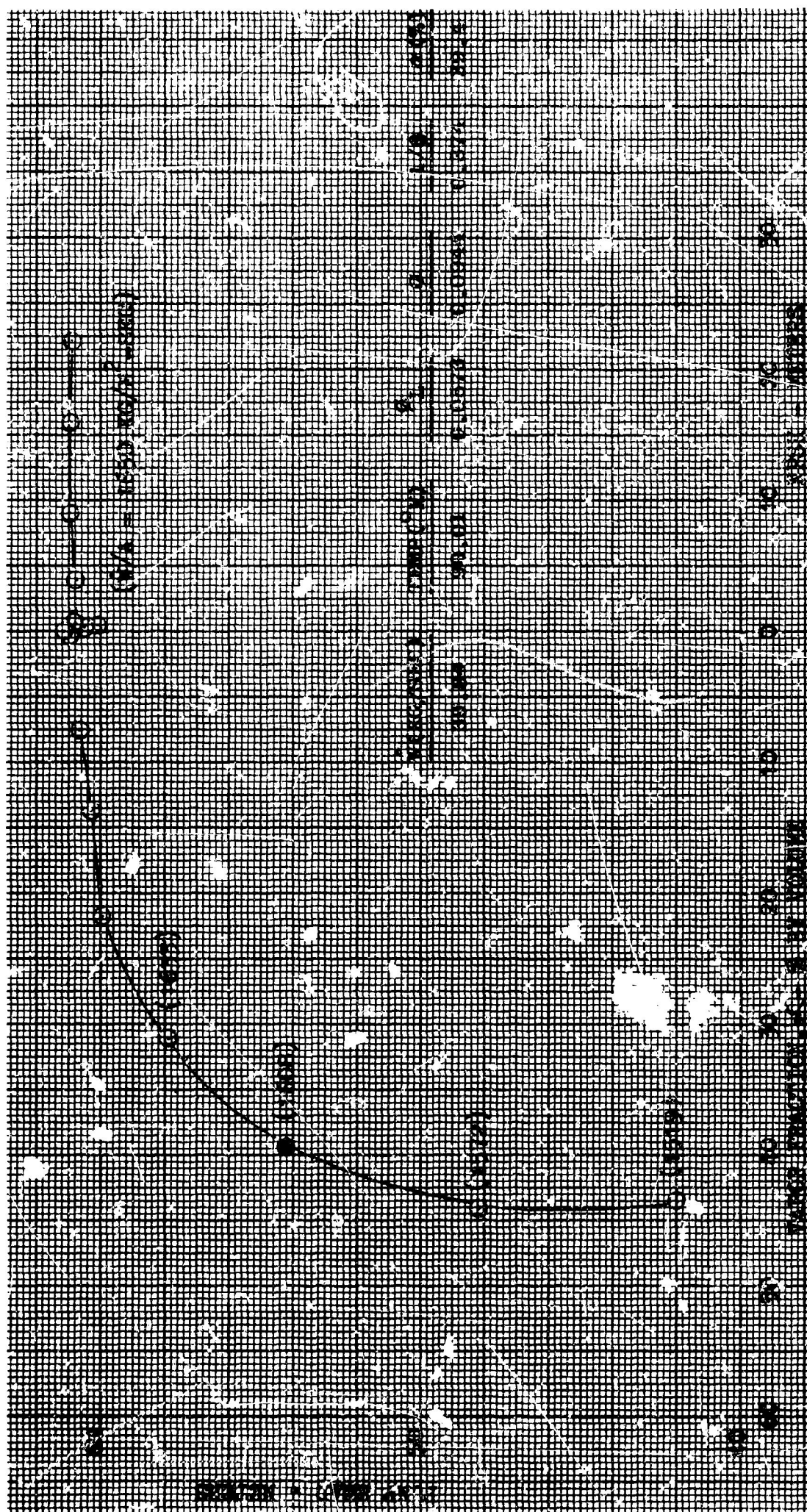




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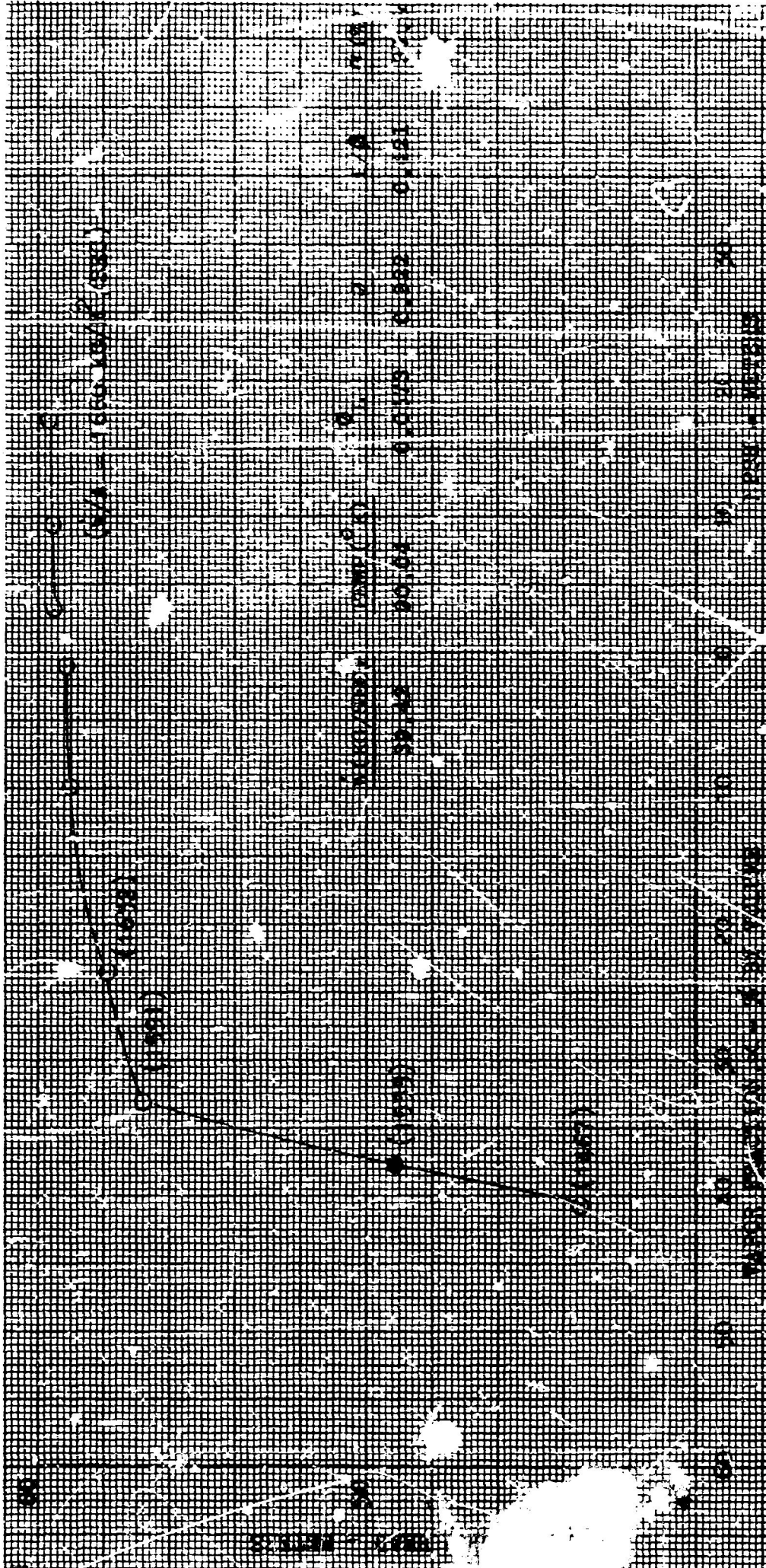


Figure 40. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713029,  
Test Speed = 261 rad/sec



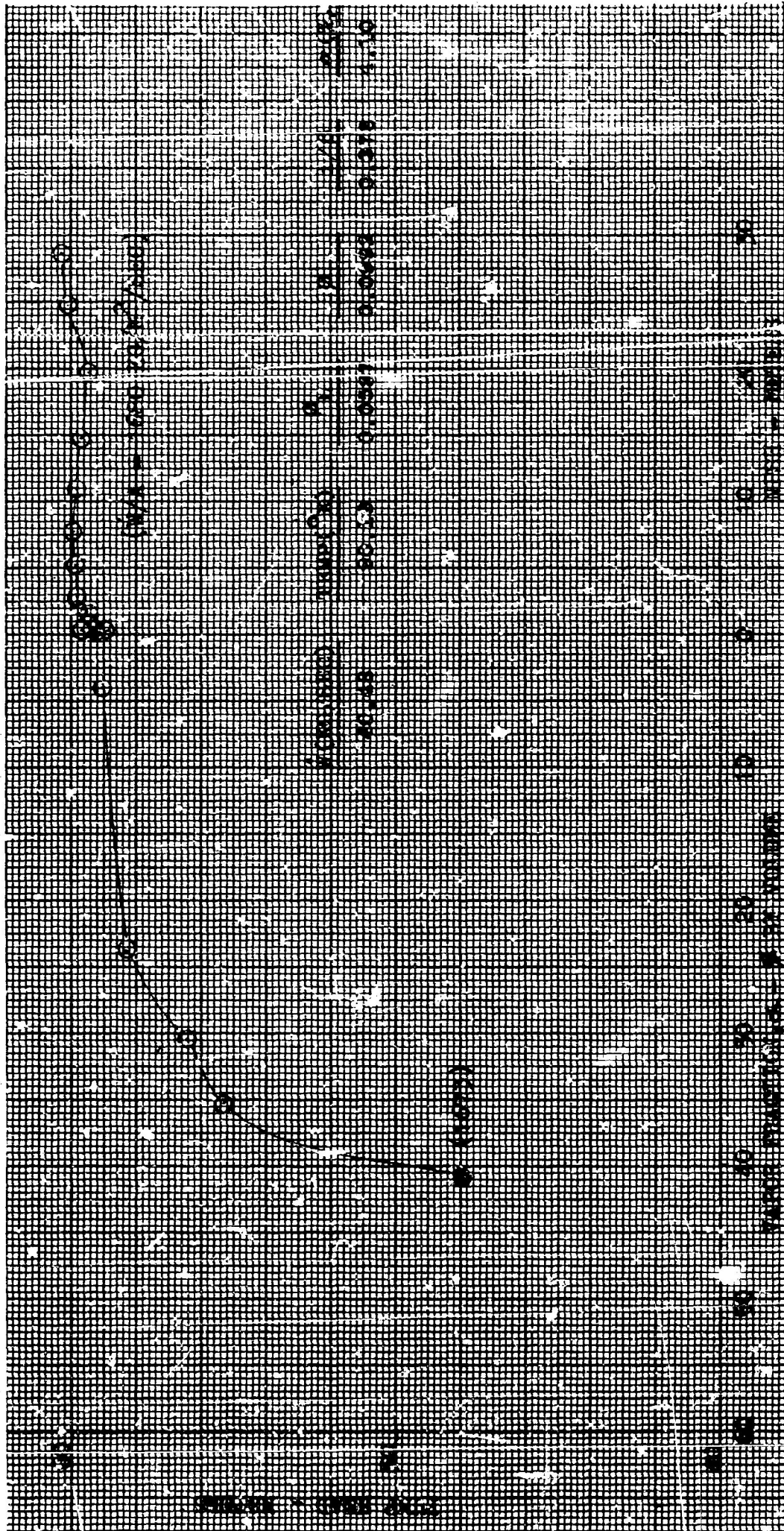


Figure 41. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713035, Test Speed = 262 rad/sec





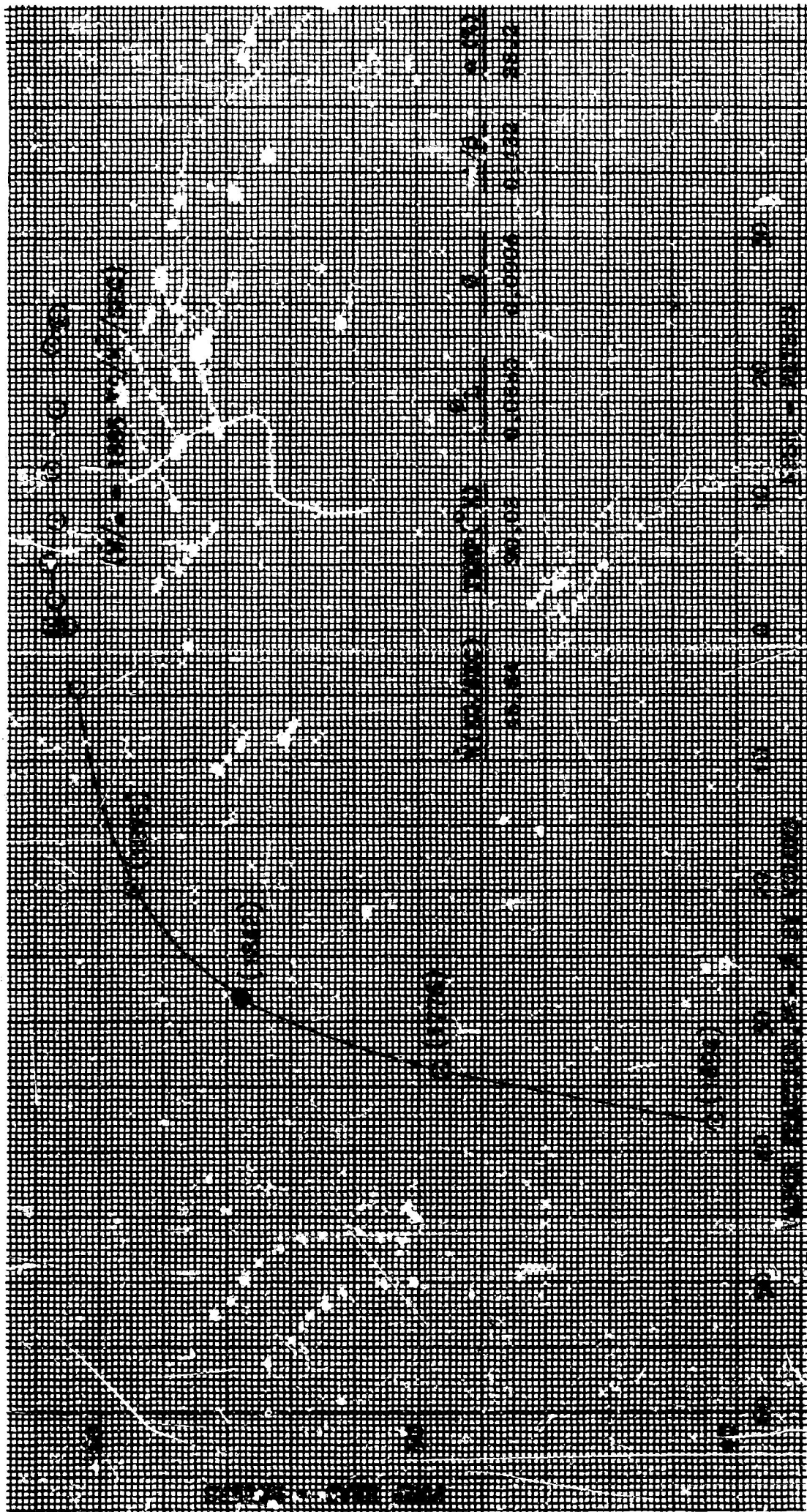


Figure 43. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713038,  
Test Speed = 267 rad/sec



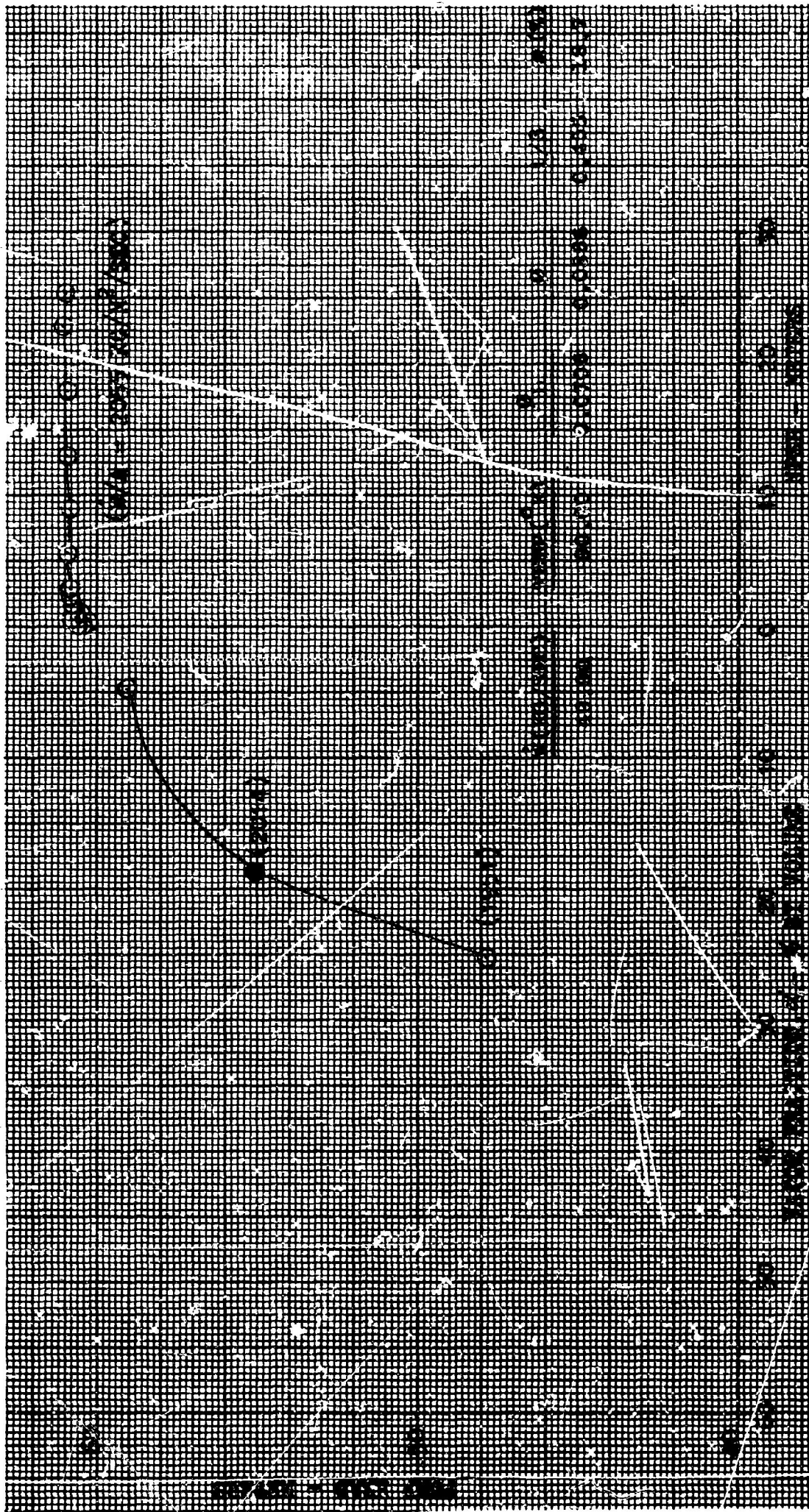


Figure 44. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713045,  
Test Speed = 269 rad/sec

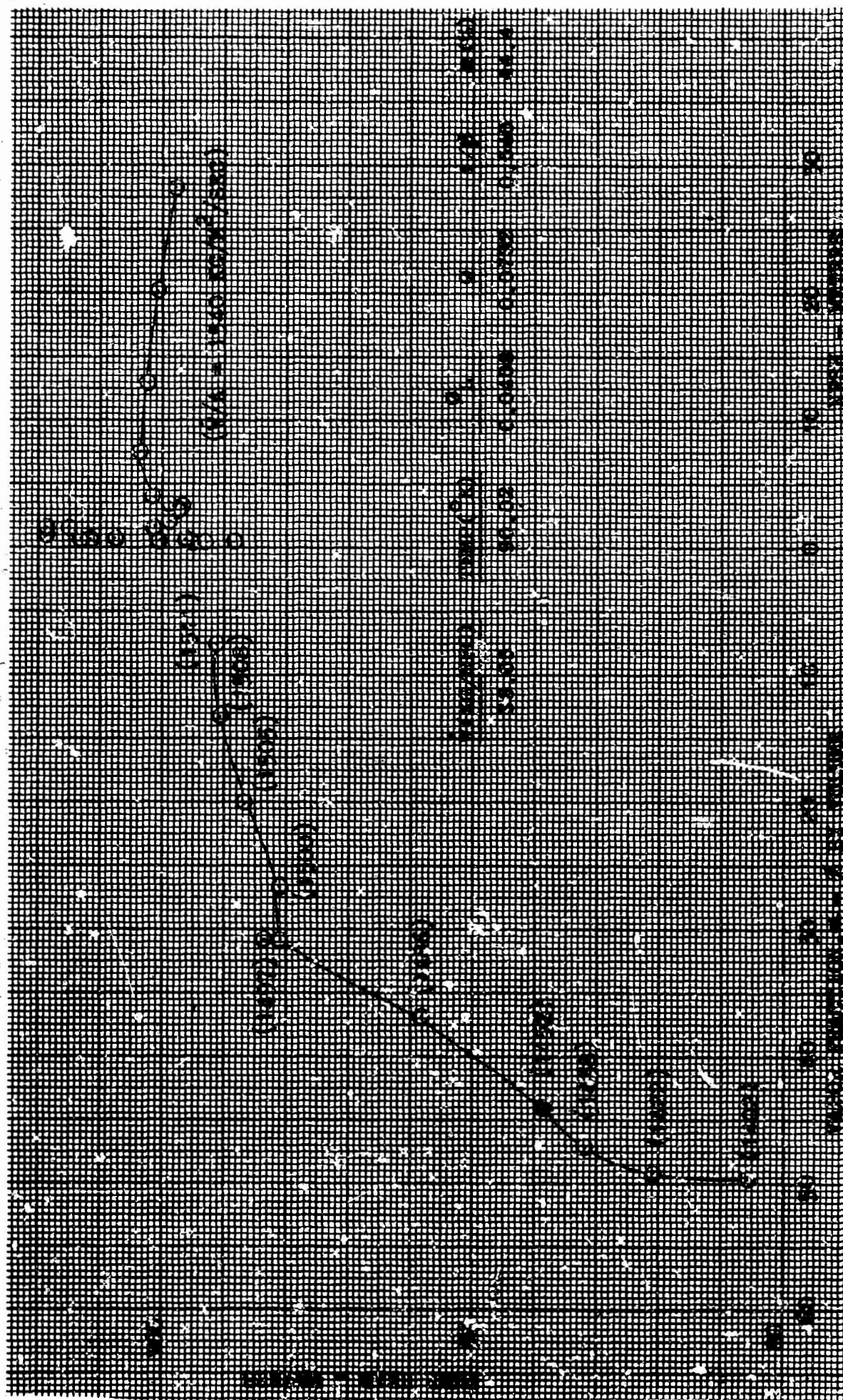


Figure 45. Mark 20-0 Two-Phase Oxygen Test Data, Run No. 713031,  
Test Speed = 335 rad/sec



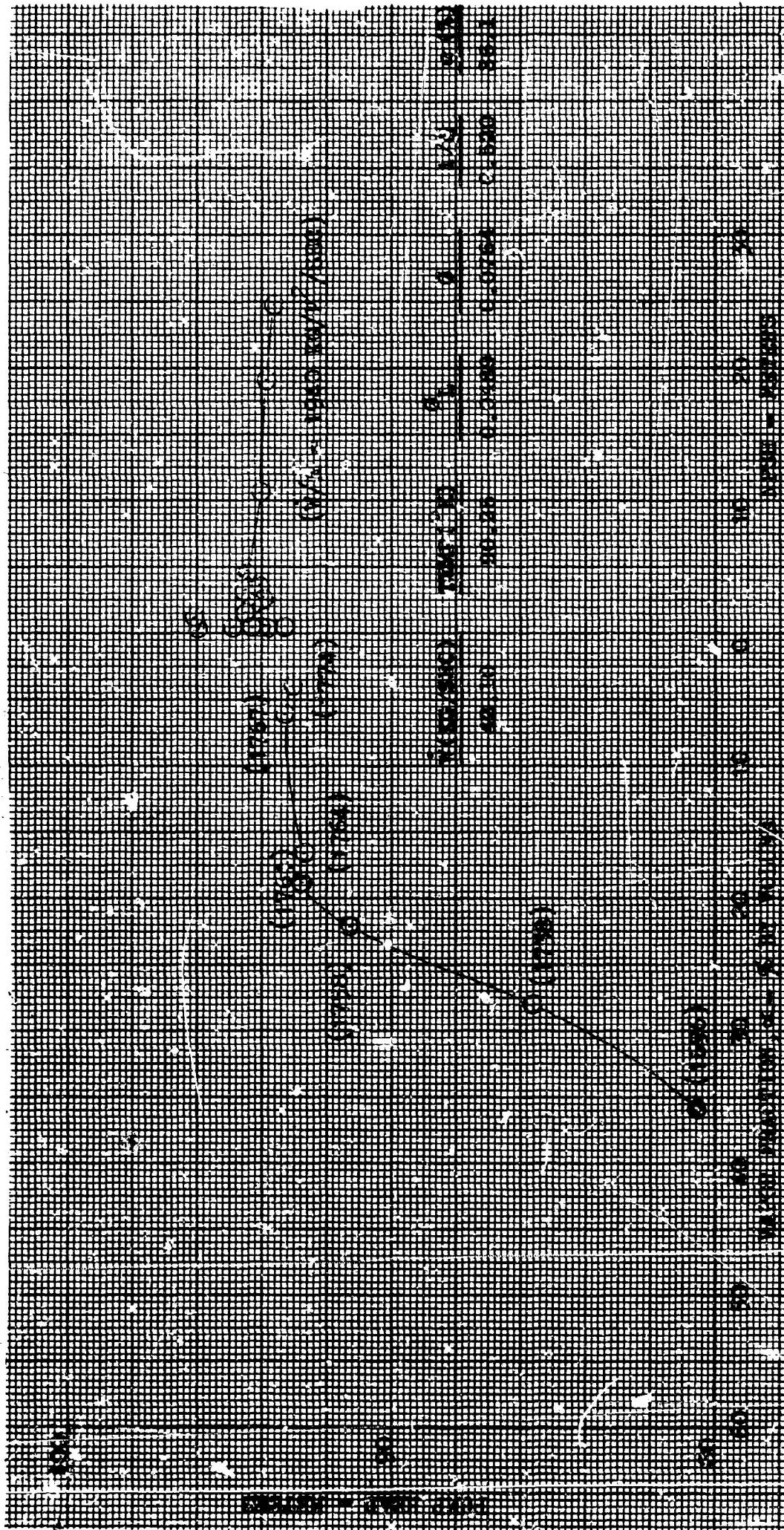


Figure 46. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713033,  
Test Speed = 327 rad/sec



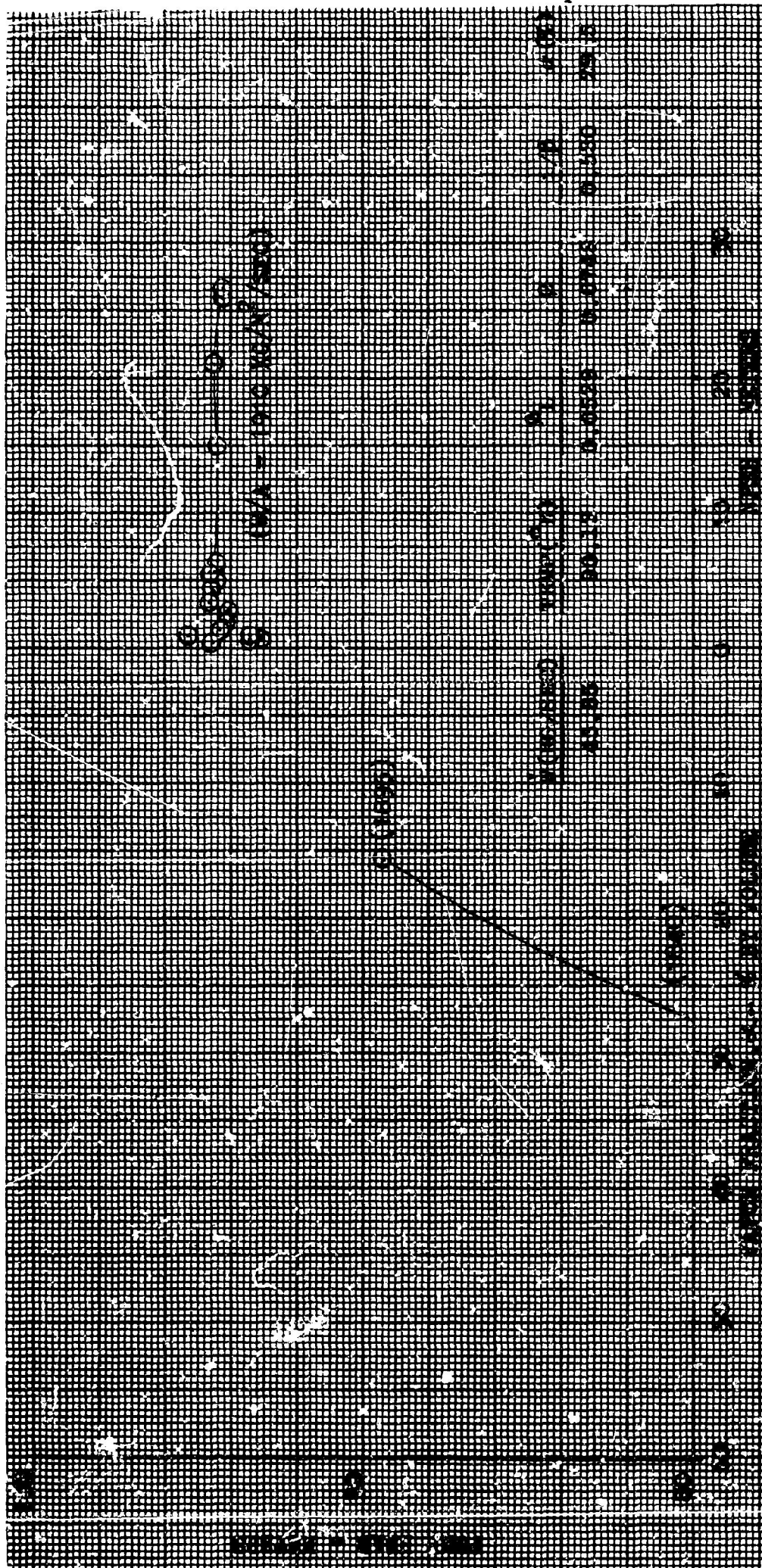


Figure 47. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713040,  
Test Speed = 378 rad/sec

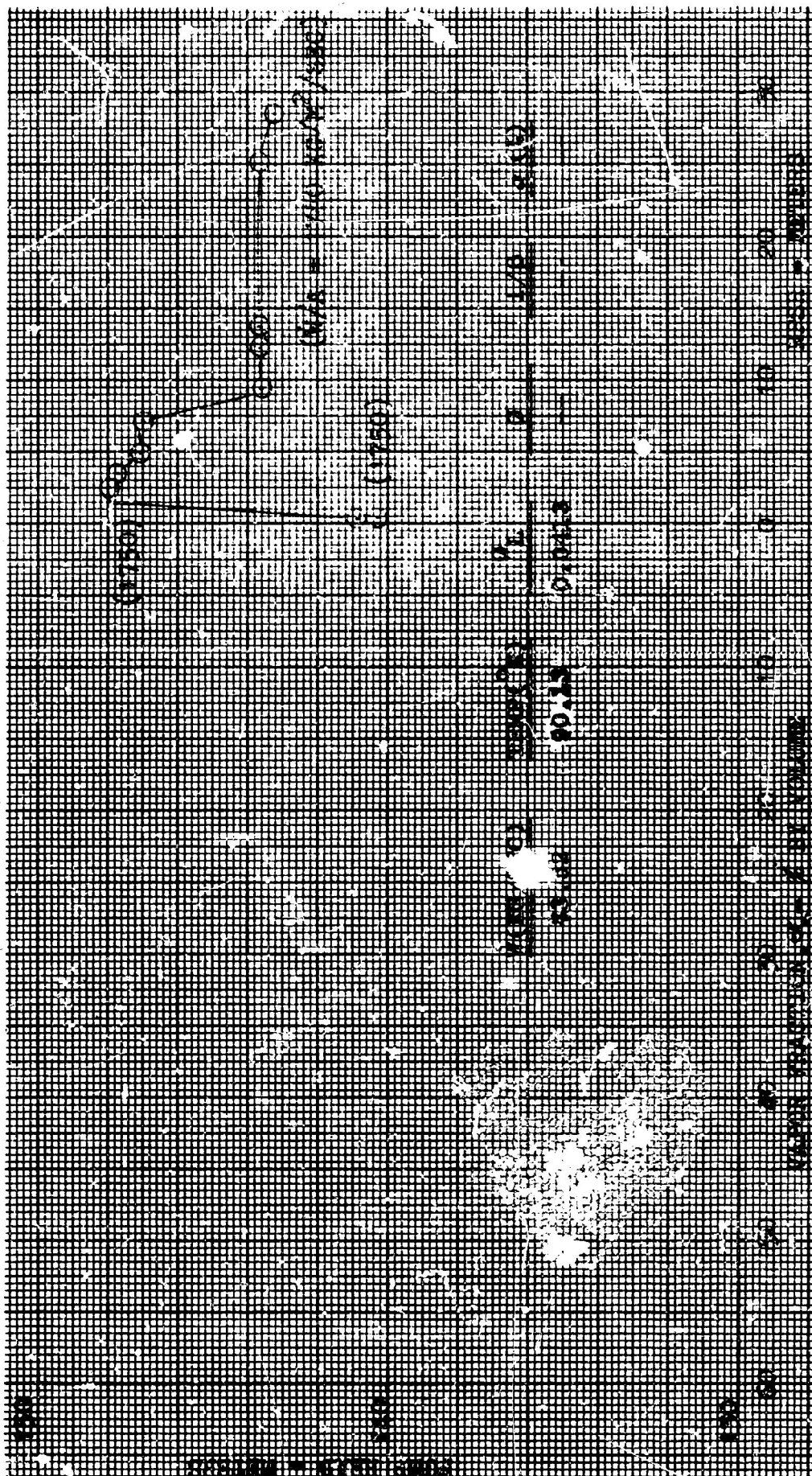


Figure 48. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713043,  
Test Speed = 401 rad/sec





Figure 49. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713034,  
Test Speed = 399 rad/sec

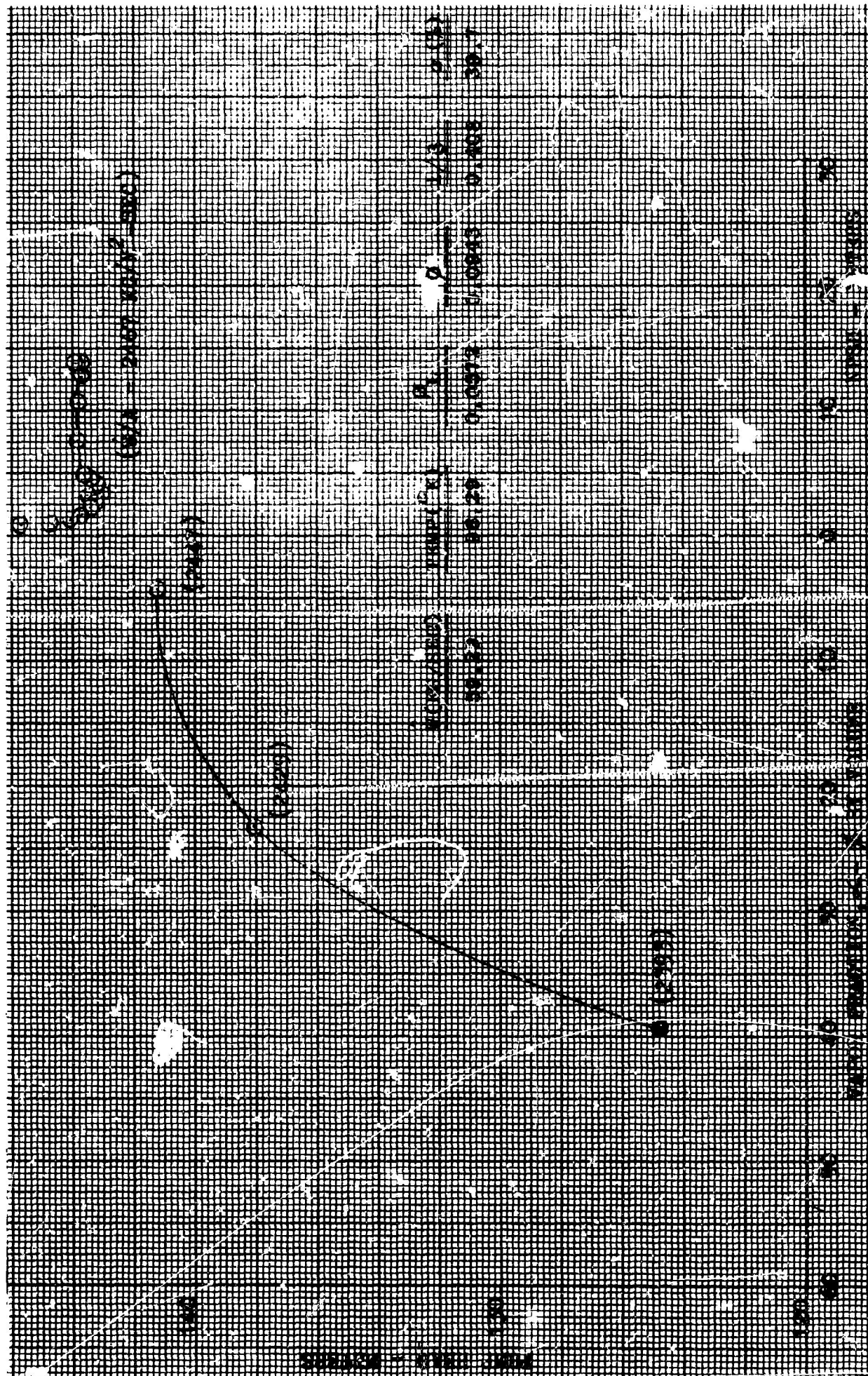


Figure 50. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713012,  
Test Speed = 406 rad/sec



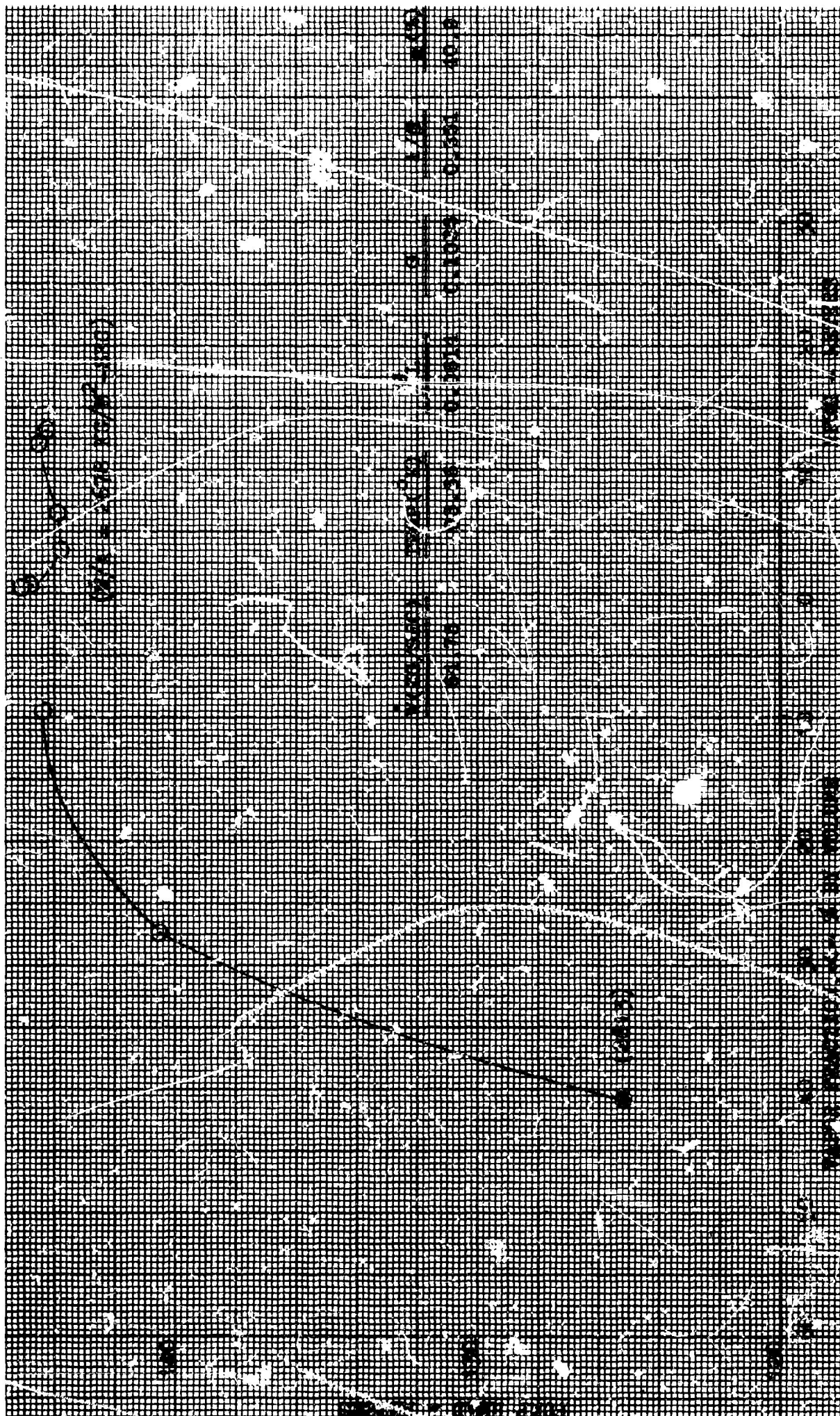


Figure 51. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713010,  
Test Speed = 408 rad/sec

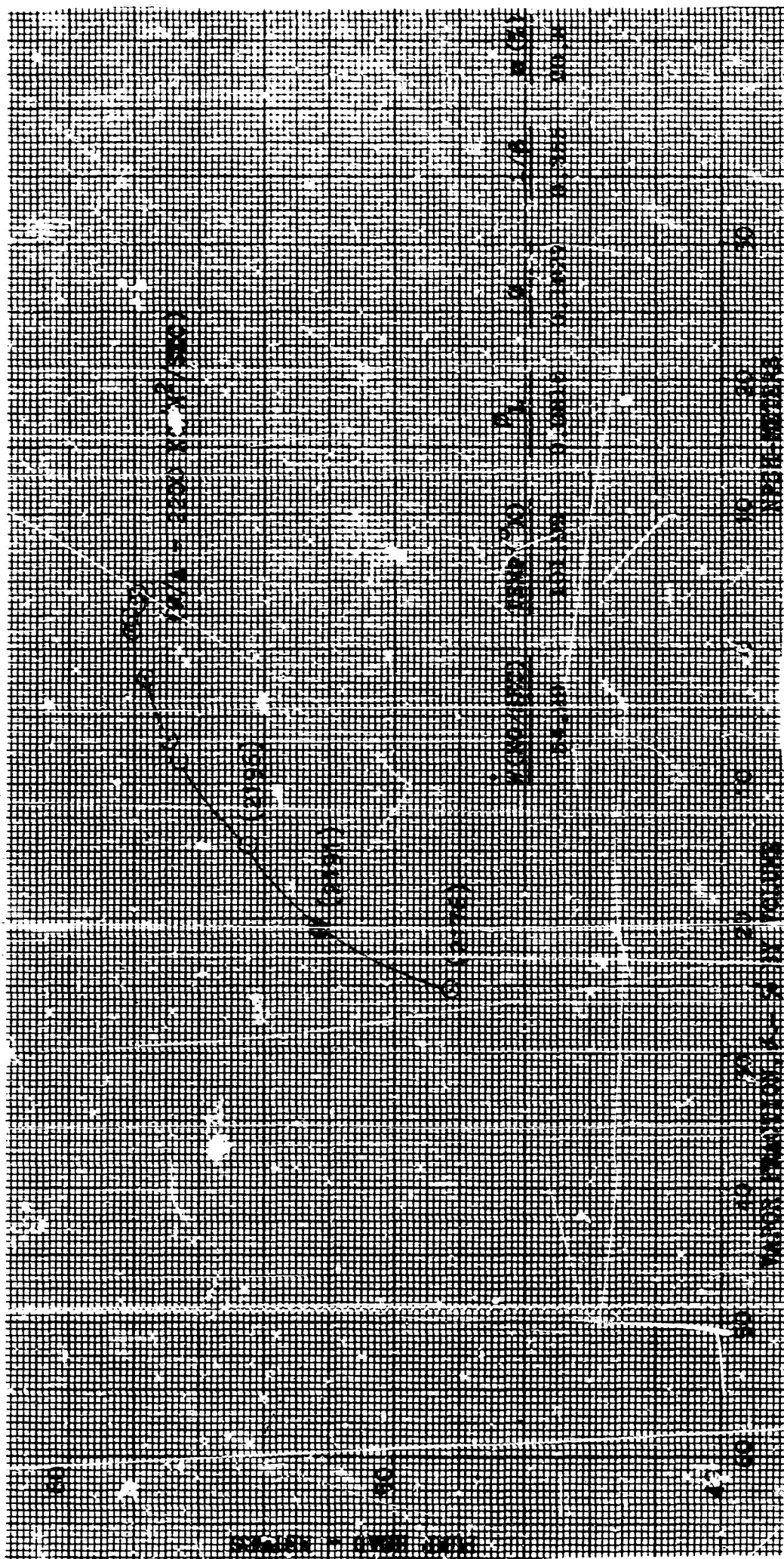


Figure 52. Mark 29-0 Two-Phase Oxygen Test Data, Run No. 713047, Test Speed = 268 Rad/Sec





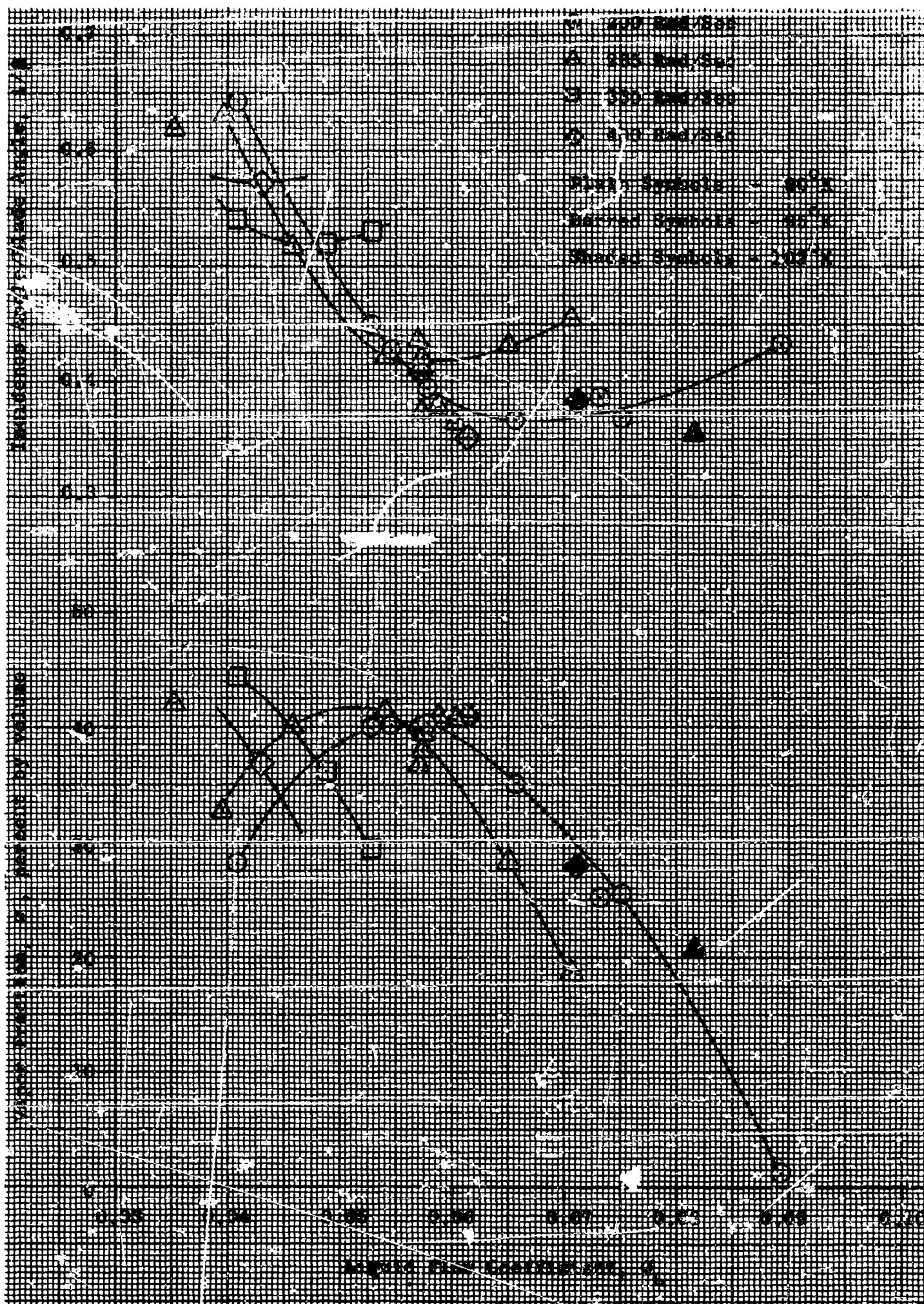


Figure 54. Two-Phase Oxygen Inducer Performance Characteristics

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This variable is a derivation of the actual flow coefficient with a substitution of the saturated liquid density for the actual density in determining the volumetric flow.

The vapor fraction ( $\alpha$ ) and incidence-to-blade angle ratio ( $i/\beta$ ) are, of course, parameters of primary interest in evaluating two-phase pumping performance.

The test summary suggests a strong influence of liquid flow coefficient, speed, and fluid temperature. For any given speed and temperature the  $i/\beta$  ratio tends to optimize at a unique  $\phi_L$  value. As the operating condition of  $\phi_L$  is reduced, the critical  $i/\beta$  ratio increases, as reflected by the tendency of  $\alpha$  to peak and drop off as  $\phi_L$  is further reduced.

The speed effect is evident in the shifting of the optimum (minimum)  $i/\beta$  ratio to progressively higher levels at a lower  $\phi_L$  as speed is increased. The maximum vapor fraction also tends to level out at a magnitude of approximately 40 percent.

The limited high-temperature data indicate a tendency to offset speed effects. The performance characteristics at higher temperatures reflect that obtained at low speed and colder temperature, regardless of speed.

Calculations also were made to present the data as if the pump were actually being tested with saturated oxygen under zero-g conditions. The first step is shown in Fig. 55, which contains theoretical plots of weight flow per unit area as a function of vapor fraction, with tank saturation temperature as a parameter. In calculating these curves it was assumed that the fluid in the tank was saturated, that it was expanded to a given velocity, and that there was an entrance loss from the tank to the pipe of 0.04 velocity head and a loss through a wide-open ball valve of 0.56 velocity head. These losses were obtained from Rocketdyne test data. The temperatures shown were calculated because the particular tests being run were at atmospheric tank pressure, which results in saturated oxygen at about 162 R.

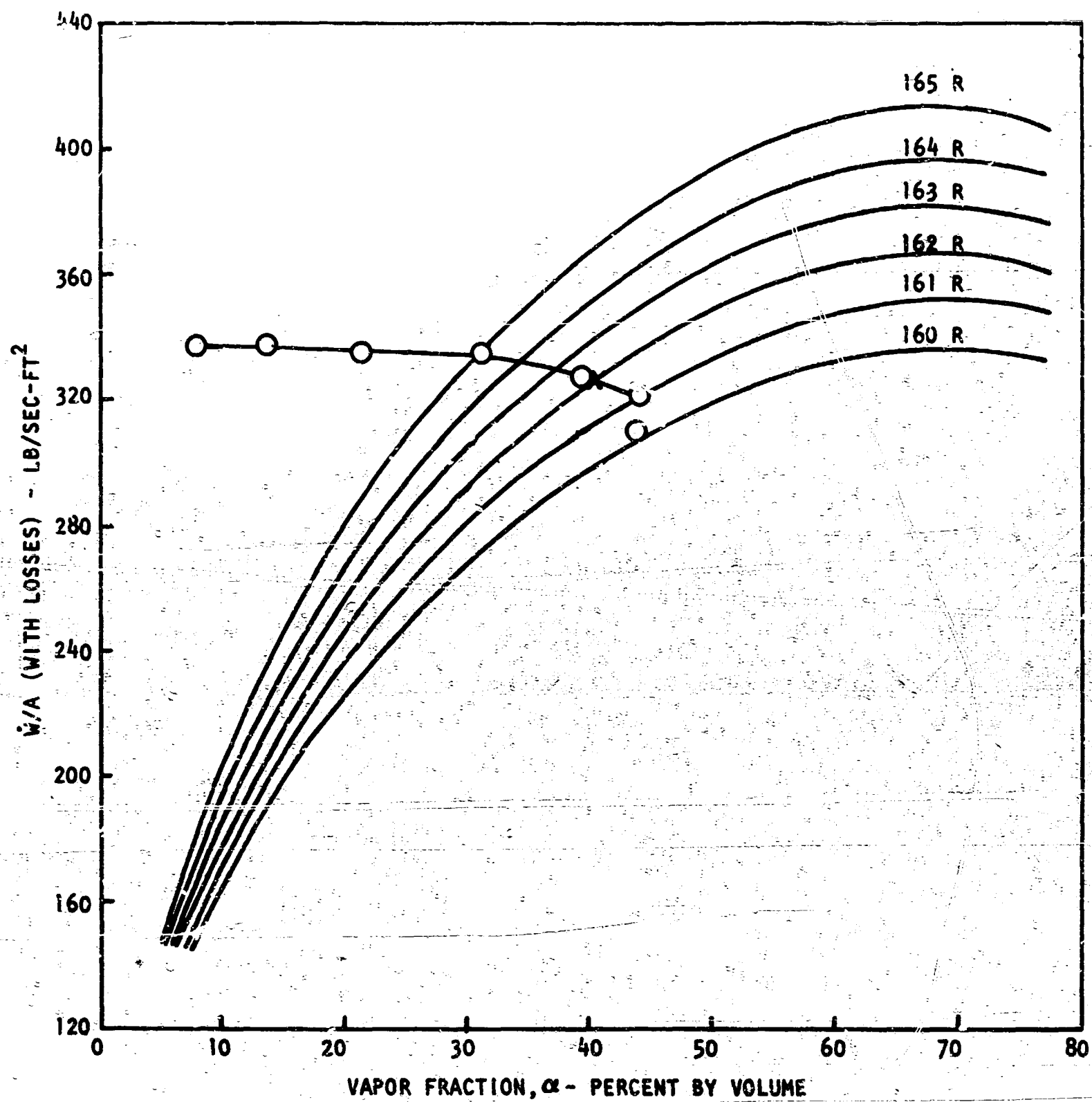


Figure 55. Equilibrium Flow Expansion From Saturated Liquid Oxygen

Shown also on Fig. 55 are test points (small circles) from a particular test run. At the point where the test line crosses the 162 F line (the inlet temperature for that particular run), the weight flow per unit area and the vapor fraction are read from the curve. Figure 39 is the basic cavitation curve for the same test run. The pump head is read from this curve at the value of the vapor fraction taken from Fig. 55 and then the head as a function of  $\dot{W}/A$  is shown as a point on Fig. 56. Figure 56, therefore, is a plot of how the pump would perform with saturated oxygen at 162 R (-296 F).

Two curves are shown at pump speeds of 1897 and 2529 rpm corresponding to pump inducer tip speeds of 60 and 80 ft/sec, which are low enough to permit a wide operating range without choking in the inlet line. The dashed lines represent the pump performance when no vapor is present. Also shown on the curve are values of  $i/\beta$ , which is the ratio of incidence-to-blade angle at the inducer tip. The curves show that the vapor fraction causes little head loss at values of  $i/\beta$  between 0.4 and 0.75.

#### COMPARISON OF DATA WITH MODEL PREDICTIONS

##### Blockage Factors

When predicting the two-phase performance of the Mark 29 oxygen inducer, an inlet line blockage factor of 7 percent was used. This value was required in Ref. 1 to make the predicted inlet line choking limit agree with the two-phase hydrogen data. There has been no subsequent indication of a different value because the Mark 25 two-phase hydrogen (Ref. 1) and the Mark 29 two-phase oxygen pumps were not tested under inlet line choking conditions. Consequently, there was no reason to deviate from the 7-percent value established in Ref. 1.

A blade blockage of 20 percent was estimated from the drawing of the Mark 29 oxygen inducer. Since a blade plus blade boundary layer of 40 percent was indicated in Fig. 4c, the boundary layer blockage on the blade would be 20 percent. Consequently, this value was used in the analysis even though it is larger than the 3- and 4-percent boundary layer blockage values observed for the Mark 15 and 25 two-phase hydrogen pumps, respectively.

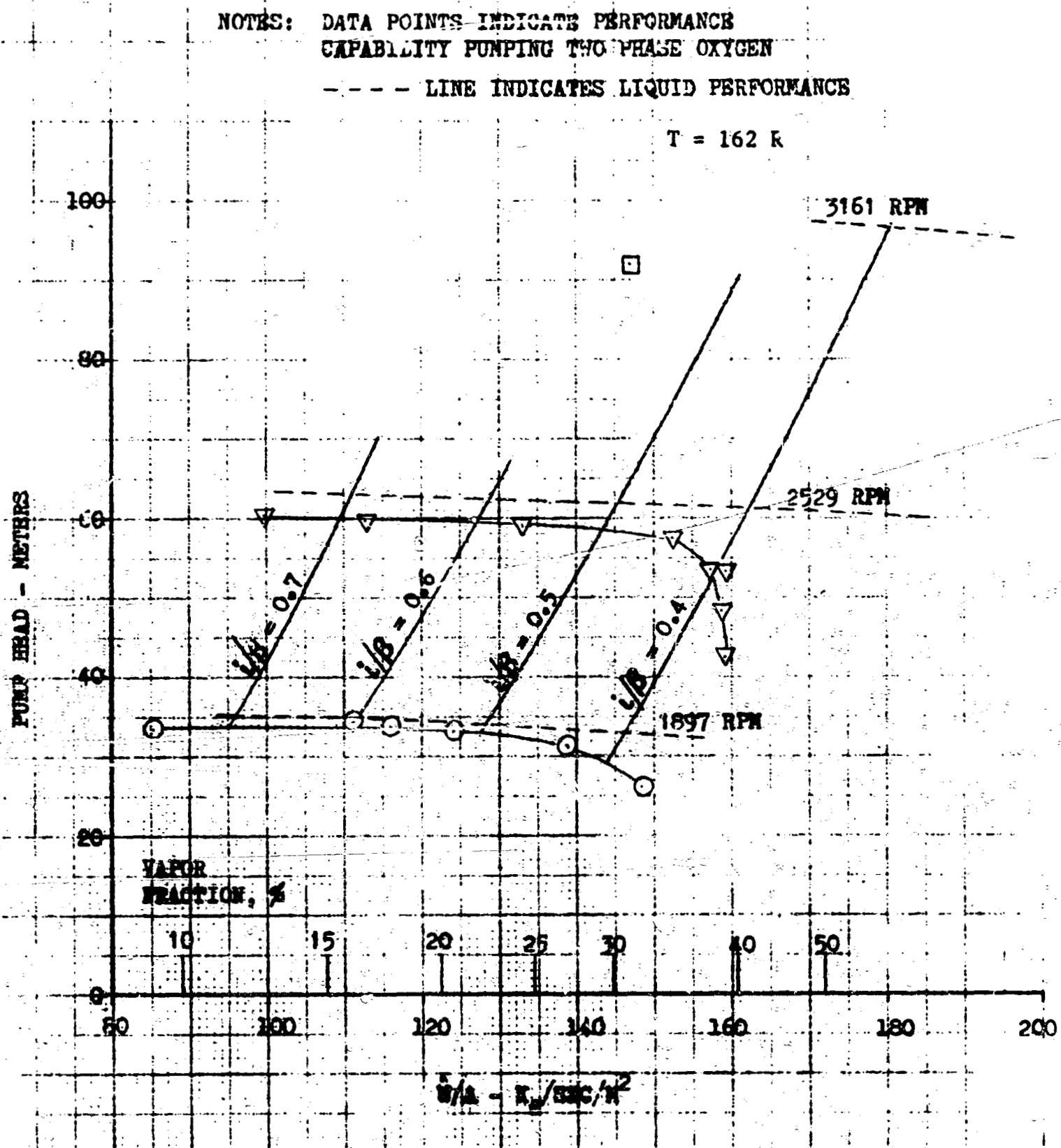


Figure 56. Mark 29-0 Two-Phase Oxygen Data (Fluid Temperature = 90 K)



The J-2 Mark 15 oxygen pump was recently tested under two-phase conditions by NASA at Marshall Space Flight Center and was found to have small inducer blade boundary layer blockage values similar to the two hydrogen inducers. Therefore, the oxygen propellant properties do not appear to be the reason for the large blockage found for the Mark 29 oxygen inducer. Because the Mark 29 oxygen inducer has a shroud and the other inducers do not, the boundary layer on the shroud was then estimated. It was found to create an additional blockage of approximately 7 percent, a value that accounts for a sizable portion of the large blockage factor found for the Mark 29 oxygen inducer. This would indicate that shrouded, two-phase inducers should be more carefully designed, since blockage has a large effect on vapor pumping capacity (Fig. 4). Other factors that could affect the total blockage include (1) the large liquid-to-vapor density ratio of oxygen (255 in 15 psia oxygen versus 53.3 in 15 psia hydrogen), which would cause a larger vapor cavity in oxygen than in hydrogen, and (2) geometrical differences in inducers. Therefore, the total blockage factor warrants further study.

#### Comparison With Inducer Pressure Rise Data

Blockage factors of 7 percent for the inlet line, 20 percent for the blades, and 20 percent for the boundary layer were used to predict the Mark 29 oxygen inducer static pressure rises under the test operating conditions. These predicted values are compared with the test values in Fig. 57 through 63 and the predictions are evaluated in Table 2. Because the test results at relative Mach numbers close to 1 (Runs 31, 34, and 10) were much droopier than the predictions using the hydrogen mathematical model (Table 1), the assumption of condensation across normal shock waves was removed, thereby producing the oxygen model listed in Table 1. Two possible reasons for this difference are (1) the oxygen inducer is a lower head design and, therefore, would provide less time for condensation to occur, and

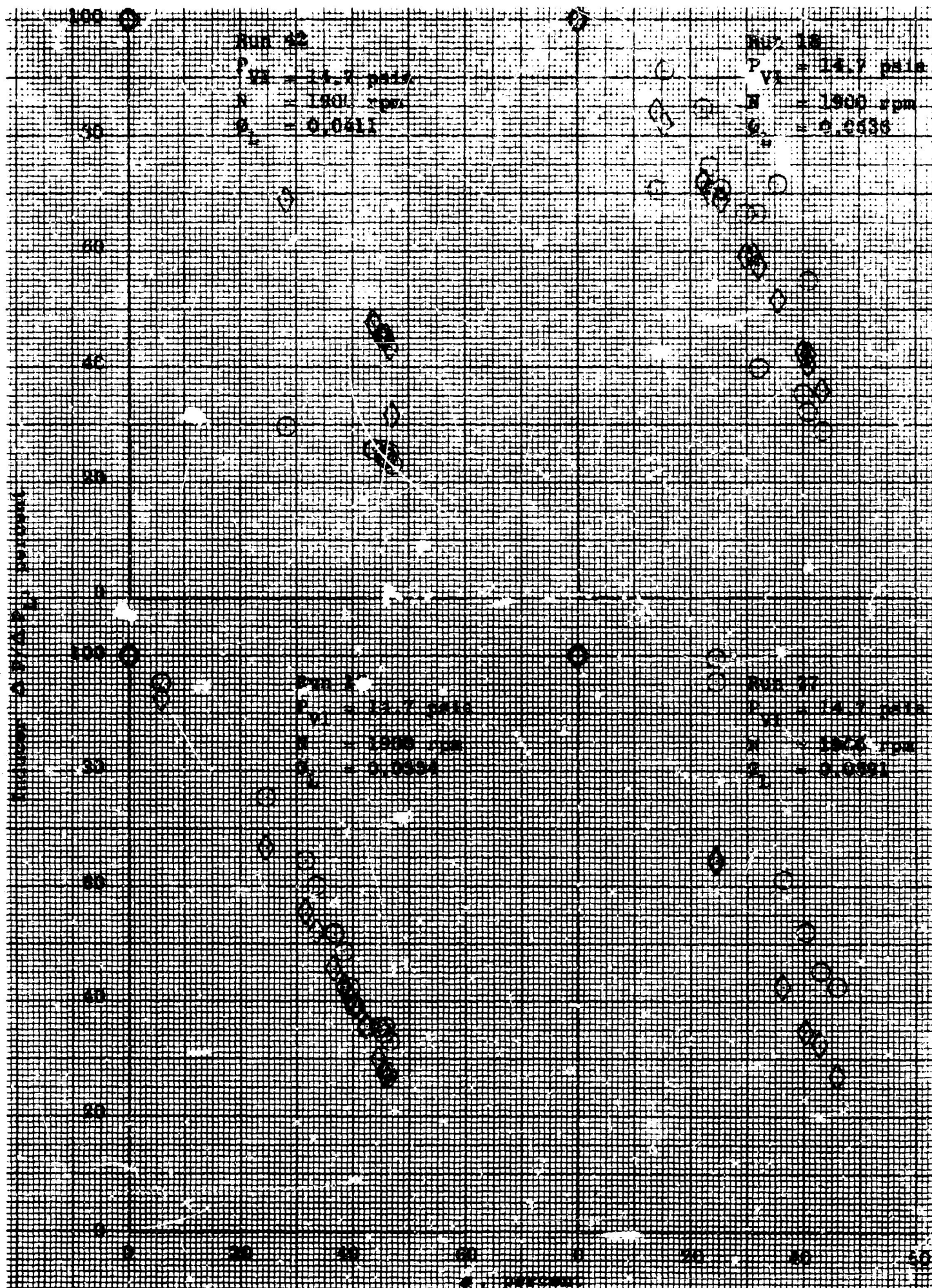


Figure 57. Predicted Values ( $\diamond$ ) of Static Pressure Ratio Compared with Test Data ( $\circ$ )

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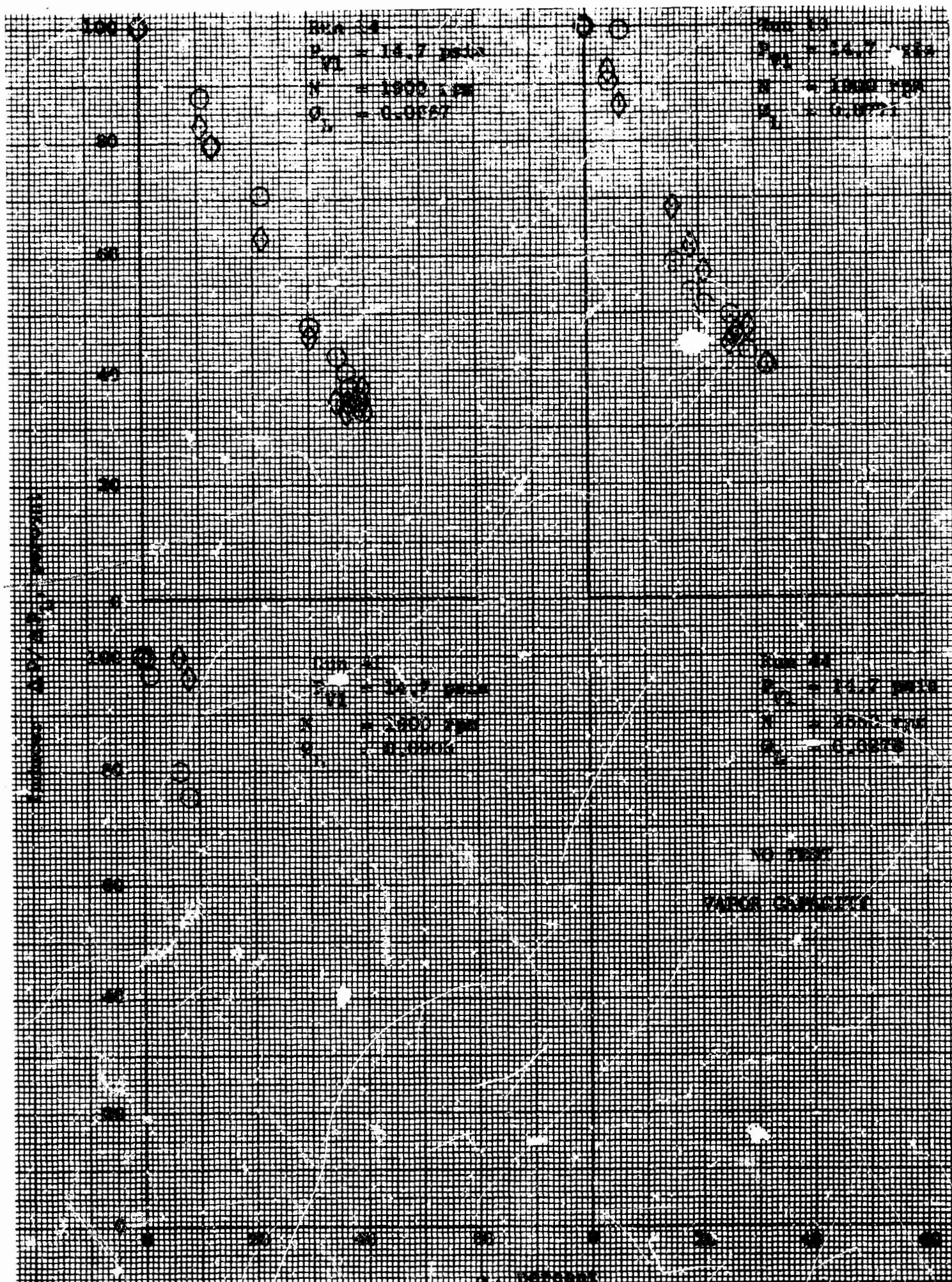


Figure 58. Predicted Values ( $\diamond$ ) of Static Pressure Ratio Compared with Test Data (O)

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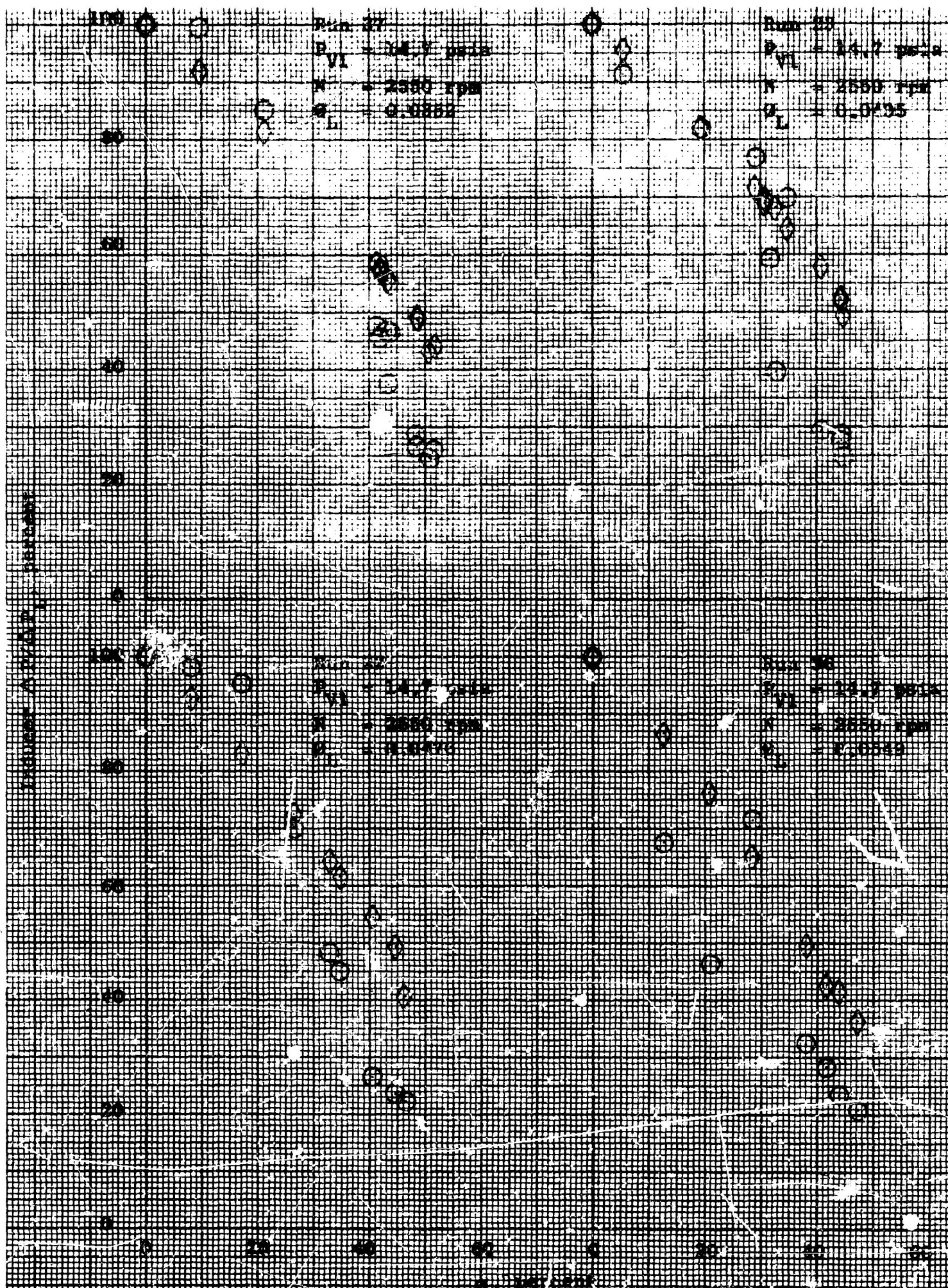


Figure 59. Predicted Values ( $\diamond$ ) of Static Pressure Ratio Compared with Test Data (O)

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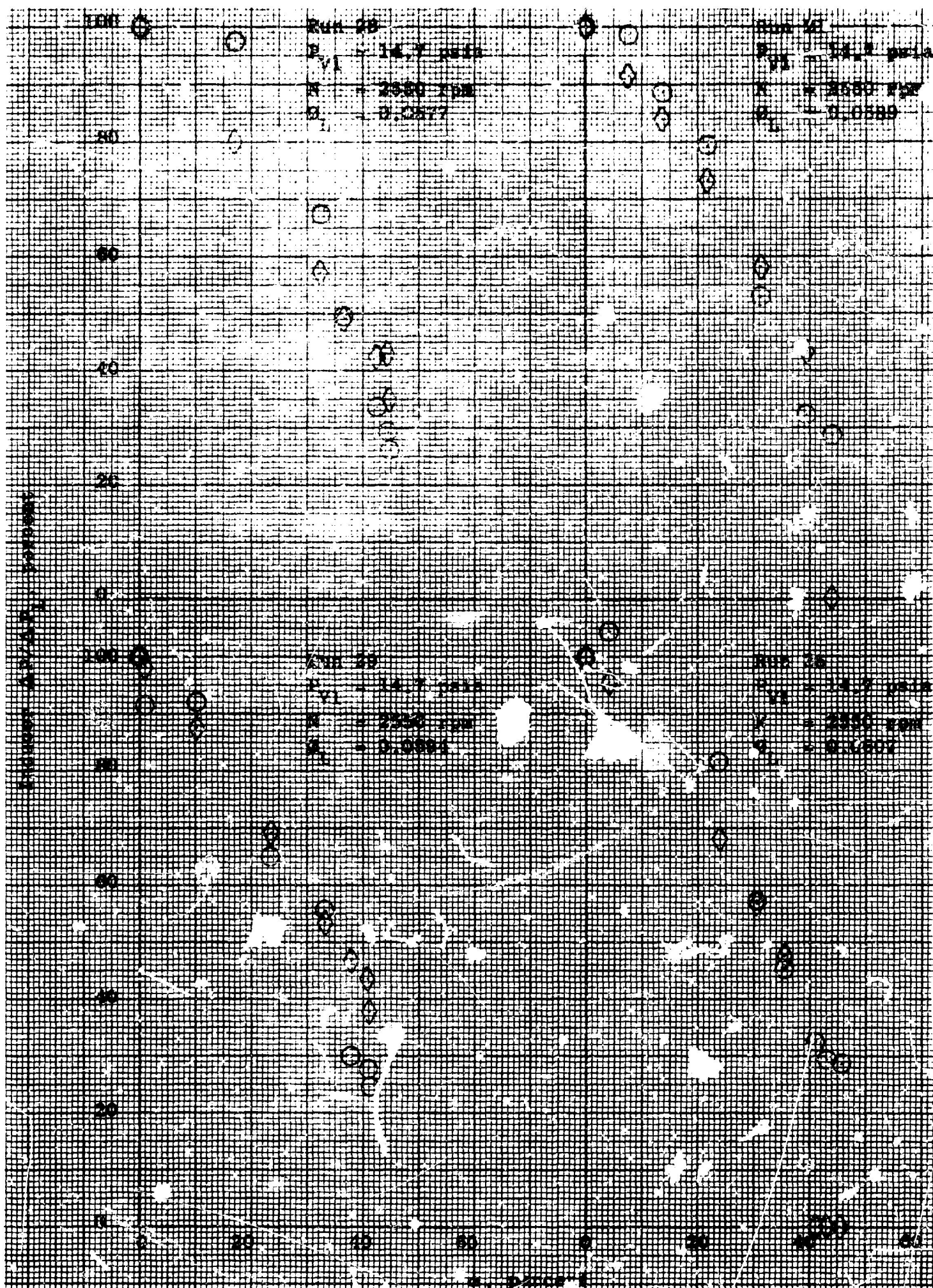


Figure 60. Predicted Values (◊) of Static Pressure Ratio Compared with Test Data (O)

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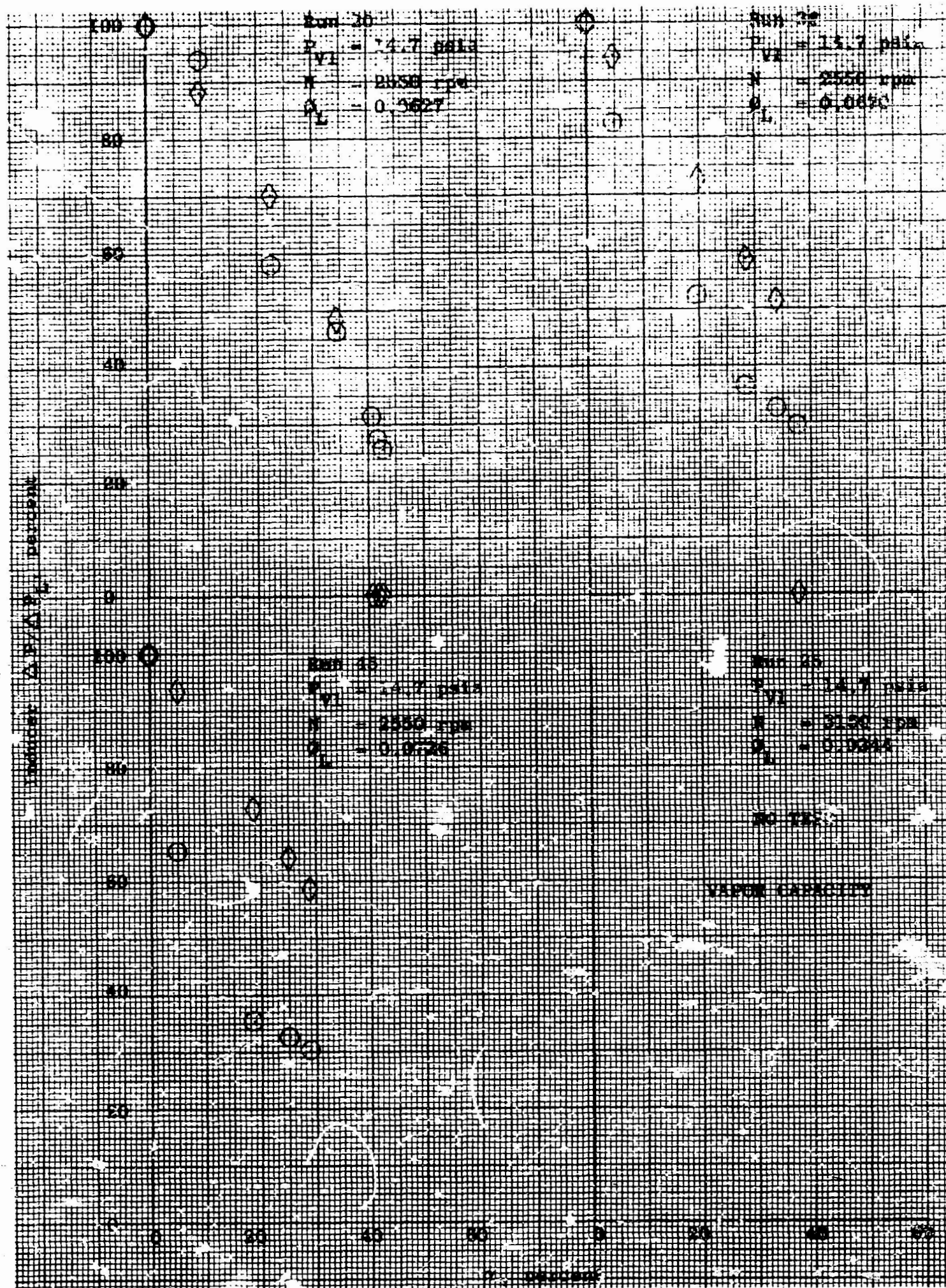
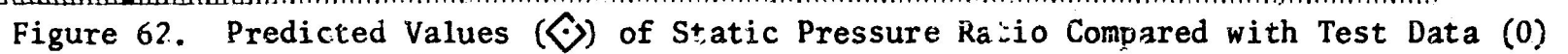


Figure 61. Predicted Values ( $\diamond$ ) of Static Pressure Ratio Compared with Test Data ( $\circ$ )







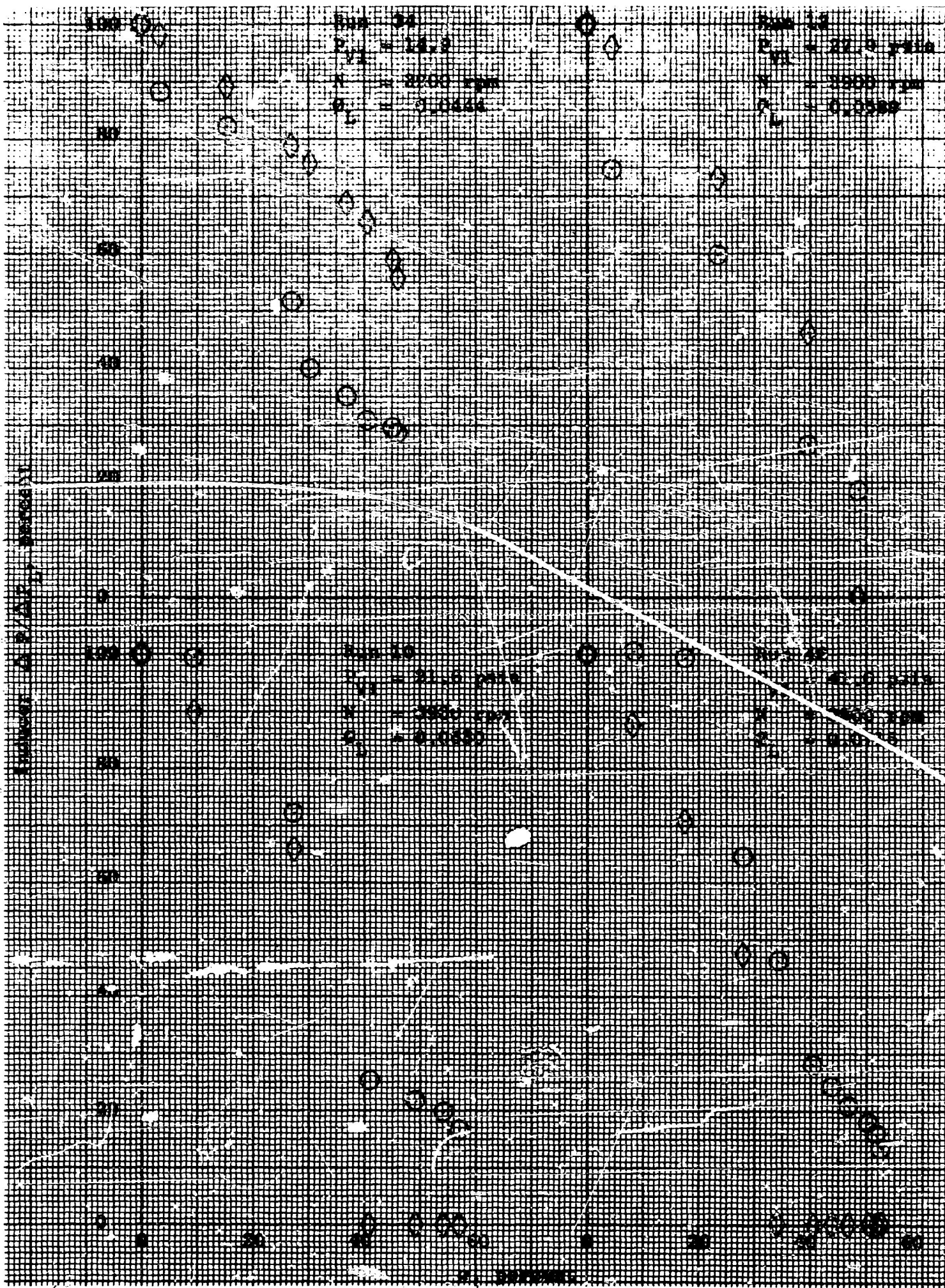


Figure 63. Predicted Values ( $\diamond$ ) of Static Pressure Ratio Compared with Test Data (O)

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TABLE 2. COMPARISON OF TEST RESULTS WITH PREDICTIONS

| Run No. | N    | PV Line | Initial $\phi_L$ | Correlation    | M <sub>Line</sub> | Maximum, M <sub>im-rel</sub> | Average, (i/β) <sub>L</sub> | α (at 20% Pump Head Loss) | Remarks About Correlation         |
|---------|------|---------|------------------|----------------|-------------------|------------------------------|-----------------------------|---------------------------|-----------------------------------|
| 42      | 1900 | 14.7    | 0.0411           | Poor           | 0.39              | 0.61                         | 0.74                        | 0.356                     | High i/β                          |
| 18      |      |         | 0.0536           | Good           | 0.51              | 0.61                         | 0.66                        | 0.408                     |                                   |
| 17      |      |         | 0.0554           | Good           | 0.52              | 0.60                         | 0.65                        | 0.414                     |                                   |
| 27      |      |         | 0.0591           | Fair           | 0.56              | 0.61                         | 0.64                        | 0.41                      |                                   |
| 14      |      |         | 0.0667           | Good           | 0.66              | 0.62                         | 0.60                        | 0.37                      |                                   |
| 13      |      |         | 0.0771           | Good           | 0.72              | 0.56                         | 0.54                        | 0.266                     |                                   |
| 41      |      |         | 0.0903           | Poor           | 0.83              | 0.33                         | 0.45                        | 0.046                     | High M <sub>Line</sub> , Low ΔP   |
| 44      | 2550 | 14.7    | 0.0278           | Poor           | --                | --                           | 0.82                        | 0                         | High i/β                          |
| 37      |      |         | 0.0352           | Good for α<40% | 0.45              | 0.81                         | 0.78                        | 0.495                     | High i/β                          |
| 23      |      |         | 0.0405           | Good for α<30% | 0.53              | 0.82                         | 0.75                        | 0.450                     | High i/β                          |
| 22      |      |         | 0.0476           | Good for α<30% | 0.61              | 0.83                         | 0.71                        | 0.404                     | High i/β                          |
| 36      |      |         | 0.0549           | Fair           | 0.70              | 0.81                         | 0.67                        | 0.431                     |                                   |
| 28      |      |         | 0.0577           | Good           | 0.73              | 0.81                         | 0.66                        | 0.405                     |                                   |
| 21      |      |         | 0.0589           | Good           | 0.74              | 0.81                         | 0.64                        | 0.434                     |                                   |
| 29      |      |         | 0.0594           | Good           | 0.75              | 0.79                         | 0.64                        | 0.384                     |                                   |
| 35      |      |         | 0.0607           | Good           | 0.76              | 0.80                         | 0.64                        | 0.406                     |                                   |
| 20      |      |         | 0.0627           | Good           | 0.80              | 0.82                         | 0.63                        | 0.406                     |                                   |
| 38      |      |         | 0.0670           | Fair           | 0.85              | 0.79                         | 0.60                        | 0.338                     | High M <sub>Line</sub>            |
| 45      |      |         | 0.0726           | Poor           | 0.94              | 0.75                         | 0.56                        | 0.243                     | High M <sub>Line</sub>            |
| 25      | 3150 | 14.7    | 0.0344           | Poor           | --                | --                           | 0.80                        | 0                         | High i/β                          |
| 31      |      |         | 0.0426           | Good for α<30% | 0.68              | 1.04                         | 0.74                        | 0.495                     | High i/β                          |
| 33      |      |         | 0.0512           | Good for α<20% | 0.78              | 0.96                         | 0.69                        | 0.360                     |                                   |
| 40      |      |         | 0.0550           | Poor           | 0.85              | 0.96                         | 0.66                        | 0.276                     | High M <sub>Line</sub>            |
| 47      | 2550 | 44.6    | 0.0817           | Good           | 0.41              | 0.40                         | 0.49                        | 0.254                     |                                   |
| 34      | 3900 | 14.9    | 0.0444           | Poor           | 0.84              | 1.22                         | 0.73                        | 0.300                     | High M <sub>Line</sub> , High i/β |
| 12      |      | 27.9    | 0.0589           | Good           | 0.67              | 0.90                         | 0.64                        | 0.400                     |                                   |
| 10      |      | 21.6    | 0.0630           | Good           | 0.90              | 1.06                         | 0.63                        | 0.410                     | High M <sub>Line</sub> (?)        |
| 46      |      | 41.0    | 0.0715           | Good           | 0.60              | 0.76                         | 0.59                        | 0.280                     |                                   |

(2) the fact that the inlet blade relative Mach number for a given inlet line Mach number is lower in oxygen than in hydrogen causes weaker normal shock waves.

As shown in Table 2, the correlation between test and predicted vapor-pumping capacities was good for the runs in which the liquid incidence-to-blade angle ratios  $(i/\beta)_L$  were less than 0.7 and the inlet line Mach numbers were less than 0.83. This 0.7  $(i/\beta)_L$  limit is a considerable improvement over the 0.6 value assumed earlier (Ref. 1) because it will permit vapor capacities up to 60 percent as opposed to less than 50 percent for the 0.6 value (Fig. 4). In addition, this increases by approximately 30 percent the liquid flow coefficient range over which vapor can be pumped (Fig. 5). A possible reason for data deviation at line Mach numbers greater than 0.83 is that this value is high enough to permit choking to occur in some localized areas where flow passage contours change. Another possible reason is that the inducer discharge static pressure measurements were taken at a place where the leakage through the impeller front wear ring (Fig. 15) could have affected the inducer discharge static pressure measurement.

This wear ring leakage could have also caused the deviation from predicted at high vapor fractions in Runs 37, 23, 22, 31, and 33 (Table 2 and Fig. 59 and 62). However, these five runs also had high  $(i/\beta)_L$ 's and high blade relative Mach numbers at the rms diameter. As a result, the tip Mach numbers exceeded 1.0. Because these data resemble MOD-2PG (Table 1 and Fig. 12) it is possible that the combination of supersonic flow at the tip and high  $(i/\beta)_L$  could cause the flow pattern and, therefore, the appropriate mathematical model to change.

#### Comparison With Vapor Pumping Limit Data

The predicted vapor pumping limits are compared with the test vapor fractions at 20 percent loss in overall pump head rise in Fig. 64 and 65. The predicted limit lines that have a negative slope are due to choking within the inducer blades. The lefthand vertical limit is at  $(i/\beta)_L = 0.7$ , and the righthand vertical limit

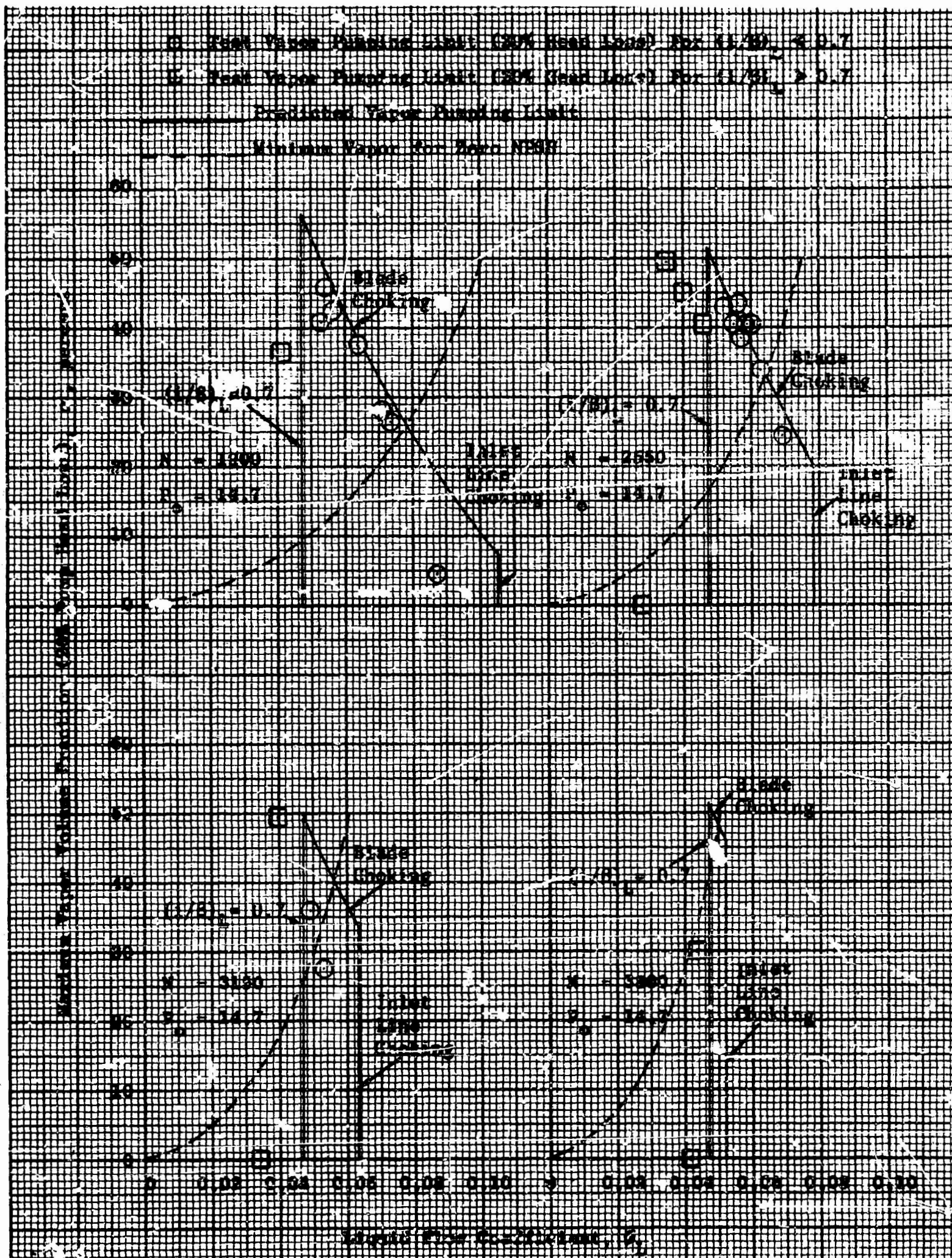


Figure 64. Predicted-Vapor Pumping Limits

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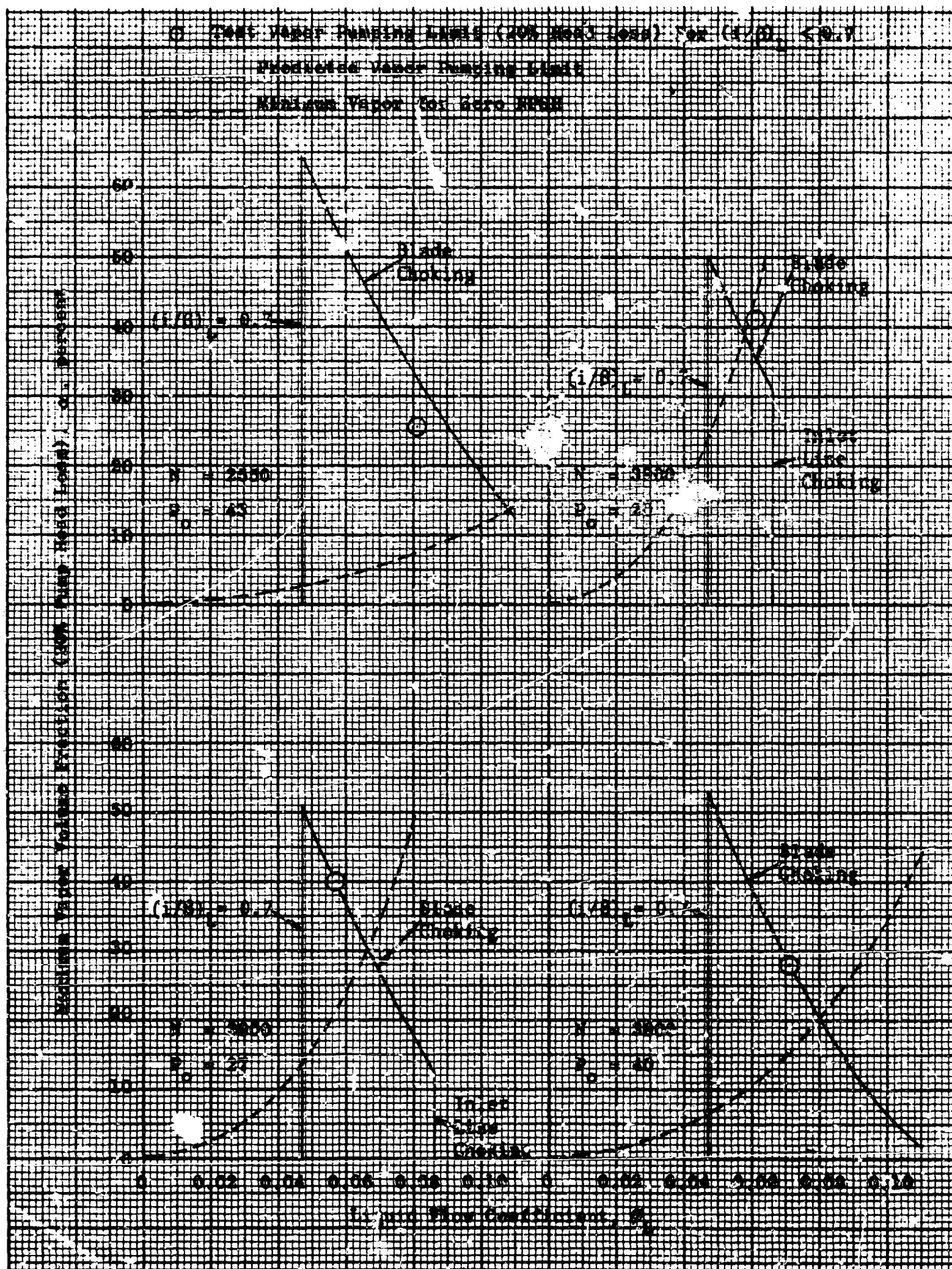


Figure 65. Predicted Vapor Pumping Limits

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is at inlet line choking. Except for the 3150- and 3800-rpm runs at 14.7 psia (Fig. 64), which have very narrow envelopes, the predicted limits show good agreement with test. The data at  $(i/\beta)_L$ 's greater than 0.7 (square symbols in Fig. 64) fall below the blade choking limit and not into any consistent pattern. The vapor pumping capability appears to be zero for  $(i/\beta)_L$ 's greater than 0.8 and to be somewhat random between  $(i/\beta)_L$ 's of 0.7 and 0.8. As is also shown, none of the tests were conducted under inlet line choking conditions. An additional conclusion to be drawn from Fig. 64 and 65 is that all but four of the test runs with  $(i/\beta)_L$ 's less than 0.7 reached a zero NPSH pumping capability.

## RECOMMENDATION

The Two-Phase Oxygen Inducer Program has contributed significantly to the technology of pumping two-phase oxygen flow. Under this program a mathematical model, design criteria, and correlation with oxygen test data have been developed. Based on the results obtained, additional areas of study have been identified that will greatly enhance this initial effort to develop the two-phase oxygen pumping technology.

1. The accuracy of the mathematical model for predicting two-phase oxygen pumping performance will be greatly improved if both the constant enthalpy and the momentum solution methods for solving isentropic expansion can be made to produce consistent results.
2. The relationship of thermodynamic suppression head (TSH) and two-phase pumping should be established to obtain a generalized performance prediction method. The TSH factor currently depends on specific inducer cavitation test data. A generalized prediction method will obviate the need for extensive testing of each new inducer.
3. Both boundary layer blockage factor of shrouded inducers and the  $(i/\beta)_L$  factor must be further investigated. The blockage factor is critical to the vapor pumping capacity of the inducer and the vapor pumping capacity at high  $(i/\beta)_L$  is critical to vapor pumping at inducer low off-design flow coefficient operation, which occurs during a rapid pump start transient.

## NOMENCLATURE

|                     |                                                                            |
|---------------------|----------------------------------------------------------------------------|
| A                   | Area normal to flow direction, $m^2$ (ft) <sup>2</sup>                     |
| B                   | Flow area blockage fraction at inducer blade overlap                       |
| $B_I$               | Inlet line boundary layer blockage fraction                                |
| c                   | Acoustic velocity, m/s (ft/s)                                              |
| $c_x$               | Acoustic velocity at constant quality, m/s (ft/s)                          |
| $c_m$               | Axial velocity, m/s (ft/s)                                                 |
| $c_s$               | Specific heat for gas at constant entropy, Btu/lb-R                        |
| g                   | Acceleration of gravity, $m/s^2$ (ft/s <sup>2</sup> )                      |
| h                   | Blade-to-blade spacing normal to blade, m (in.)                            |
| h                   | Enthalpy, J/K <sub>g</sub> (Btu/lb)                                        |
| $\Delta H$          | Pump total head rise, m (ft)                                               |
| i                   | Inducer blade incidence angle, rad (deg)                                   |
| $i/\beta$           | Flow incidence to blade angle ratio                                        |
| $(i/\beta)_L$       | Incidence to blade angle ratio using initial liquid flow value to obtain i |
| $(i/\beta)_{L-des}$ | $(i/\beta)_L$ at the design point                                          |
| J                   | Joule's equivalent of heat (778.2), ft-lb/Btu                              |
| M                   | Mach number                                                                |
| N                   | Rotational speed, rad/s (rpm)                                              |
| NPSH                | Net positive suction head, m (ft)                                          |
| P                   | Static pressure, N/cm <sup>2</sup> (psi)                                   |
| $P_t$               | Total pressure, N/cm <sup>2</sup> (psi)                                    |
| Q                   | Volume flowrate, m <sup>3</sup> /s (gpm)                                   |
| R                   | Gas constant, J kmole <sup>-1</sup> K <sup>-1</sup> (ft-lb/lb-R)           |
| T                   | Temperature, K (R)                                                         |

|            |                                                                                                        |
|------------|--------------------------------------------------------------------------------------------------------|
| TSH        | Thermodynamic suppression head, m (ft)                                                                 |
| t          | Inducer blade thickness, m (in.)                                                                       |
| U          | Inducer blade tangential velocity (at tip diameter unless otherwise specified), m/s (ft/s)             |
| V          | Flow velocity, m/s (ft/s)                                                                              |
| $\dot{W}$  | Mass flowrate, kg/s (lb/s)                                                                             |
| X          | Quality (percent vapor by weight)                                                                      |
| $\alpha$   | Vapor fraction (percent vapor by volume)                                                               |
| $\beta$    | Blade angle relative to tangent, rad (deg)                                                             |
| $\gamma_s$ | Specific heat ratio for gas at constant entropy                                                        |
| $\lambda$  | Inducer hub-tip ratio                                                                                  |
| $\rho$     | Flow density, kg/m <sup>3</sup> (lb/ft <sup>3</sup> )                                                  |
| $\phi$     | Inducer flow coefficient $C_m/U$ (unless otherwise specified, the two-phase value at the tip diameter) |
| $\psi$     | Head coefficient, $g\Delta H/U^2$                                                                      |

#### SUBSCRIPT

|    |                                                                                                                                           |
|----|-------------------------------------------------------------------------------------------------------------------------------------------|
| 0  | Tank conditions                                                                                                                           |
| 1  | At inlet or upstream of shock wave; condition at beginning of increment; upstream of the screen in the 10-inch section                    |
| 01 | Total value at station 1                                                                                                                  |
| 2  | At discharge or downstream of shock wave; condition being calculated at end on increment; downstream of the screen in the 7-inch section. |
| 02 | Total value at station 2                                                                                                                  |
| A  | Point on the saturation line at which $h = h_{02}$                                                                                        |
| B  | Beginning of increment                                                                                                                    |
| C  | End of increment                                                                                                                          |



|       |                                                        |
|-------|--------------------------------------------------------|
| D     | Point on T-S chart at which $s = s_{LA}$ and $P = P_2$ |
| f     | Fluid                                                  |
| L     | Liquid phase                                           |
| L-des | Liquid phase (at design point)                         |
| Lm    | Liquid phase at mean (rms)                             |
| LT    | Liquid phase at tip                                    |
| Line  | In inlet line                                          |
| m     | Meridional; mean (rms)                                 |
| max   | Maximum value                                          |
| min   | Minimum value                                          |
| rel   | Relative                                               |
| s     | At constant entropy; static                            |
| t     | Throat (blade overlap); total                          |
| T     | Tip                                                    |
| V     | Vapor phase                                            |
| X     | At constant quality                                    |
| *     | Choked conditions                                      |

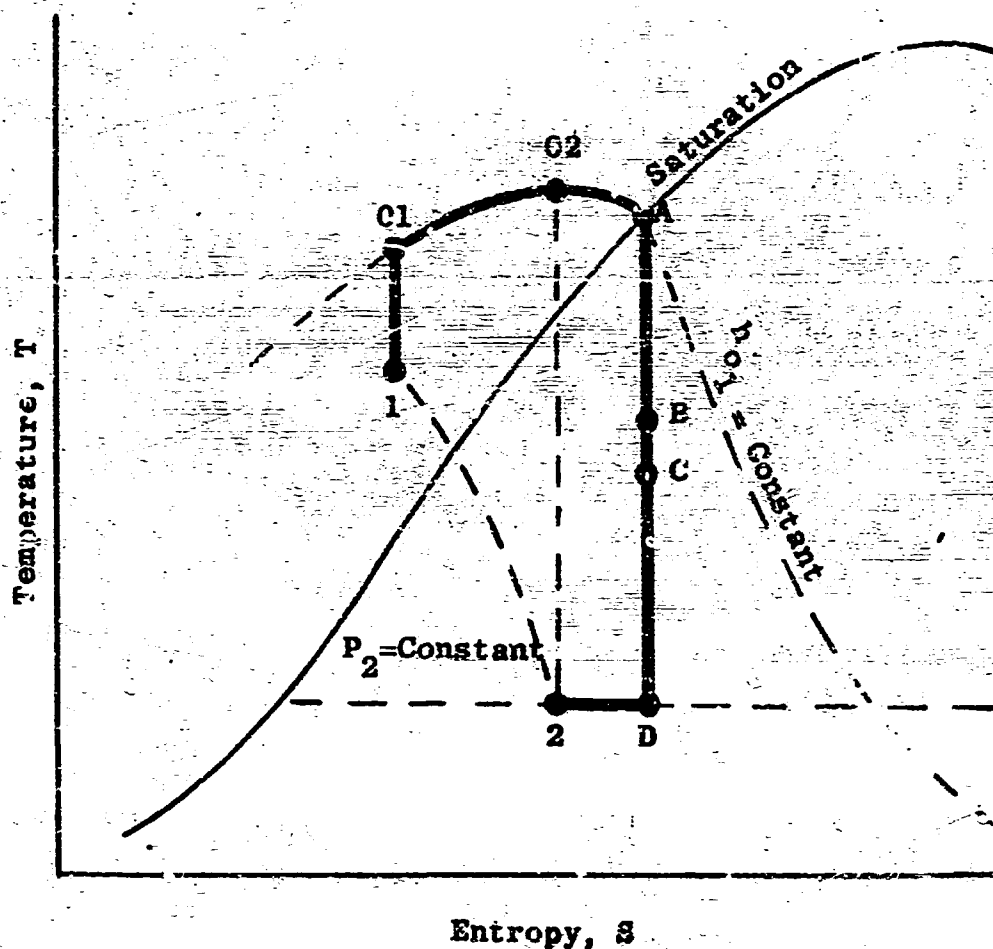
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## APPENDIX A

## MOMENTUM SOLUTION FOR REDUCING TEST DATA

The data reduction computer program was modified to solve for the vapor fraction in oxygen by means of the momentum solution. The method can be explained best by referring to the following sketch of a temperature-entropy chart for oxygen.



When flow passes from a subcooled liquid state upstream of the screen (where the line diameter is 10 inches) to a two-phase state downstream of the screen (where the line diameter is 7 inches), the static pressure decreases because

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of the higher flow velocity and the pressure loss across the screen, the static temperature decreases because of flow vaporization, and the entropy increases because of the irreversible screen pressure drop. These changes in static conditions are shown on the temperature-entropy (T-S) chart by the dashed line between points 1 and 2. As the physical distance between stations 1 and 2 on the test stand is approximately 3 feet, the heat addition to the flow is considered to be negligible. Therefore, the flow is assumed to be at constant total enthalpy and, as shown in the T-S chart, the total enthalpies at stations 1 and 2 (O1 and O2, respectively) are assumed to lie on the same enthalpy line.

The problem to be solved may be stated as follows: Given measured values of flowrate, static pressures at stations 1 and 2, and static temperature at station 1, determine the vapor volume fraction at station 2 (the vapor volume fraction entering the inducer) without using published values of enthalpy when in the two-phase region. The reason for this restriction is that flow calculations in two-phase oxygen require more accurate values of enthalpy than are available.

The first step in solving this problem is to determine the flow velocity at station 1 (Eq.A-1)

$$C_{m_1} = \frac{144 Q_1}{449 A_1} \quad (A-1)$$

It was assumed that the published values of enthalpy were sufficiently accurate to make the required calculations in the liquid region. Therefore, the enthalpy at station 1 was determined

$$h_1 = f(P_1, T_1) \quad (A-2)$$



followed by the corresponding total enthalpy

$$h_{o_1} = h_1 + \frac{C_{m_1}^2}{2gJ} \quad (A-3)$$

Station A is that point on the liquid saturation line at which the enthalpy equals  $h_{o_1}$ .

$$h_A = h_{o_1} \quad (A-4)$$

This provides sufficient information to determine  $P_A$ ,  $T_A$ , and  $S_{LA}$  from published physical properties. The enthalpy drop along an isentropic expansion from  $P_A$  to  $P_D$  ( $P_D = P_2$ ) was obtained by solving the momentum equation at finite increments beginning at station A.

$$dh = - \frac{V dV}{gJ} = - \frac{dP}{\rho} \quad (A-5)$$

Calling the beginning and the end of each increment stations B and C, respectively, calling the pressure drop across each increment  $\Delta P$ , and assuming a linear variation of density between stations B and C, we get an incremental solution by using Eq. A-6 through A-9 and a pressure drop of 0.05 psi per increment. Published physical properties were used to obtain the saturated liquid and saturated vapor values of density and entropy.

$$P_C = P_B - \Delta P \quad (A-6)$$

$$x_C = \frac{s_{LA} - s_{LC}}{s_{VC} - s_{LC}} \quad (A-7)$$

$$\rho_C = \frac{\rho_{LC}}{1 + x_C \left( \frac{\rho_{LC}}{\rho_{VC}} - 1 \right)} \quad (A-8)$$

$$\frac{V_C^2}{2g} = \frac{V_B^2}{2g} + 144 \left( \frac{P_B - P_C}{\rho_B - \rho_C} \right) \ln \left( \frac{\rho_B}{\rho_C} \right) \quad (A-9)$$

This calculation was terminated when station D was reached or, in other words, when  $P_c$  was equal to the measured downstream static pressure ( $P_2$ ). The end values for this part of the calculation are expressed by Eq. A-10 through A-12.

$$x_D = \frac{s_{LA} - s_{L2}}{s_{V2} - s_{L2}} \quad (A-10)$$

$$\frac{p_{L2}}{p_D} = 1 + x_D \left( \frac{p_{L2}}{p_{V2}} - 1 \right) \quad (A-11)$$

$$h_A - h_D = VD^2 / 2gJ \quad (A-12)$$

Energy (Eq. A-13)

$$\frac{v_2^2}{2gJ} = \frac{VD^2}{2gJ} - T_2 (s_2 - s_D) \quad (A-13)$$

and continuity (Eq. A-14)

$$\frac{v_2^2}{2gJ} = \frac{v_1^2}{2gJ} \left( \frac{p_{L1}}{p_{L2}} \right)^2 \left( \frac{A_1}{A_2} \right)^2 \left( \frac{p_{L2}}{p_2} \right)^2 \quad (A-14)$$

were then combined and solved for the liquid to mixture density ratio at station 2 (Eq. A-15 through A-17).

$$\frac{p_{L2}}{p_2} = -\frac{Y}{2} + \sqrt{\left(\frac{Y}{2}\right)^2 + \frac{p_{L2}}{p_D} Y + Z} \quad (A-15)$$

where:

$$Y = \left( \frac{p_{L2}}{p_{L1}} \frac{A_2}{A_1} \right)^2 \left( \frac{2gJ}{v_1^2} \right) \left[ \frac{T_2 (s_{V2} - s_{L2})}{\frac{p_{L2}}{p_{V2}} - 1} \right] \quad (A-16)$$

$$Z = \left( \frac{p_{L2}}{p_{L1}} \frac{A_2}{A_1} \frac{VD}{v_1} \right)^2 \quad (A-17)$$

The final step was to solve for the quality (Eq. A-18)

$$x_2 = \frac{\frac{\rho_{L2}}{\rho_2} - 1}{\frac{\rho_{L2}}{\rho_{V2}} - 1} \quad (A-18)$$

and the vapor volume fraction (Eq. A-19)

$$\alpha_2 = \frac{1}{1 + \frac{\rho_{V2}}{\rho_{L2}} \left( \frac{1 - x_2}{x_2} \right)} \quad (A-19)$$

APPENDIX B  
TWO PHASE LOX PUMP TESTING  
CTL I-2A  
INSTRUMENTATION REQUIREMENTS

| <u>PARAMETER</u>              | <u>RANGE</u> | <u>PRECISION</u> | <u>RECORDER</u> |
|-------------------------------|--------------|------------------|-----------------|
| <u>I. LOW FREQUENCY</u>       |              |                  |                 |
| <u>PRESSURES</u>              |              |                  |                 |
| RUN TANK, PO, PSIG            | 0-100        | B                | ASTRO, DIGR     |
| SUCTION, PS1, PSIG            | 0-50         | B                | ASTRO           |
| PS2, PSIG                     | 0-50         | B                | ASTRO, DIGR     |
| PS3, PSIG                     | 0-50         | B                | ASTRO           |
| PS4, PSIG                     | 0-50         | B                | ASTRO, DIGR     |
| DISCHARGE, PD1, PSIG          | 0-500        | B                | ASTRO,          |
| PD2, PSIG                     | 0-500        | B                | ASTRO, DIGR     |
| PUMP DELTA (PD2-PS2), PSID    | 0-500        | B                | ASTRO, DIGR     |
| INDUCER DISCHARGE, PSIG       | 0-200        | B                | ASTRO           |
| PRIMARY SEAL CAVITY, PSIG     | 0-50         | B                | ASTRO           |
| REDLINE                       | 35 MAX       |                  |                 |
| BEARING COOLANT               | 0-200        | B                | ASTRO           |
| INTERMEDIATE SEAL PURGE, PSIG | 0-50         | B                | ASTRO, GAGE     |
| <u>TEMPERATURES</u>           |              |                  |                 |
| RUN TANK, TO, °F              | -300/-250    | C                | ASTRO, DIGR     |
| SUCTION, TS1, °F              | -300/-250    | C                | ASTRO           |
| TS2, °F                       | -300/-250    | C                | ASTRO, DIGR     |
| TS3, °F                       | -300/-250    | C                | ASTRO           |
| TS4, °F                       | -300/-250    | C                | ASTRO, DIGR     |
| DISCHARGE, TD1, °F            | -300/-250    | C                | ASTRO           |
| TD2, °F                       | -300/-250    | C                | ASTRO, DIGR     |
| BEARING COOLANT, °F           | -300/-250    | C                | ASTRO           |
| REDLINE                       | -260 MAX     |                  |                 |



| <u>PARAMETER</u>                                | <u>RANGE</u> | <u>PRECISION</u> | <u>RECORDER</u>       |
|-------------------------------------------------|--------------|------------------|-----------------------|
| <b>II. <u>CYCLIC</u></b>                        |              |                  |                       |
| DISCHARGE FLOW #1, GPM                          | 0-2000       | C                | ASTRO, DIGR           |
| #2, GPM                                         | 0-2000       | C                | ASTRO                 |
| PUMP SPEED, RPM                                 | 0-8000       | C                | ASTRO, DIGR           |
| DRIVE SPEED, RPM                                | 0-8000       | C                | ASTRO                 |
| <b>III. <u>DYNAMIC</u></b>                      |              |                  |                       |
| <u>PHOTOCONS (DC SUPPRESSED, C/10KC)</u>        |              |                  |                       |
| SUCTION, PH1, PSI P-P                           | 0-50         | --               | FM TAPE, BRUSH        |
| PH2, PSI P-P                                    | 0-50         | --               | FM TAPE, BRUSH        |
| DISCHARGE, PH3, PSI P-P                         | 0-500        | --               | FM TAPE, BRUSH        |
| <u>DATA SENSORS</u>                             |              |                  |                       |
| PUMP INLET, PSIG                                | 0-100        | C                | ASTRO, FM TAPE, BRUSH |
| PUMP DISCHARGE, PSIG                            | 0-500        | C                | ASTRO, FM TAPE        |
| <u>ACCELEROMETERS</u>                           |              |                  |                       |
| INLET RADIAL (12:00) 800 CPS L.P.<br>10 KC W.B. | 20 GRMS      | --               | FM TAPE, BRUSH        |
|                                                 | 200 GRMS     | --               | FM TAPE               |
| INLET RADIAL (3:00) 800 CPS L.P.<br>10 KC W.B.  | 20 GRMS      | --               | FM TAPE, BRUSH        |
|                                                 | 200 GRMS     | --               | FM TAPE               |
| INLET AXIAL (12:00) 800 CPS L.P.<br>10 KC W.B.  | 20 GRMS      |                  | FM TAPE, BRUSH        |
|                                                 | 200 GRMS     |                  | FM TAPE               |
| REAR RADIAL (12:00) 800 CPS L.P.<br>10 KC W.B.  | 20 GRMS      |                  | FM TAPE, BRUSH        |
|                                                 | 200 GRMS     |                  | FM TAPE               |

# APPENDIX C

## MARK 29-0 TWO-PHASE OXYGEN TEST SUMMARY CTL I-2A

| Run No. | Duration | Temperature | N (rpm) | Q (gpm) | Remarks                                         |
|---------|----------|-------------|---------|---------|-------------------------------------------------|
| 1       | 23.1     | -298        |         |         | C/O by erroneous speed indication.              |
| 2       | 5.1      | -298        |         |         | C/O by faulty flowmeter coil output.            |
| 3       | 3.0      | -298        |         |         | C/O by drive problems.                          |
| 4       | 189.1    | -298        | 1897    | 554     | No cavitation. Insufficient screen $\Delta P$ . |
| 5       | 3.1      | -298        | 3793    | 609     | C/O by drive problems.                          |
| 6       | 27.2     | -298        | 3793    | 609     | C/O when T/V closed.                            |
| 7       | 203.4    | -298        | 3793    | 609     | No cavitation. Insufficient screen $\Delta P$ . |
| 8       | 26.9     | -298        | 1897    | 554     | C/O by excessive screen $\Delta P$ .            |
| 9       | 93.1     | -298        | 1897    | 554     | No cavitation. Insufficient screen $\Delta P$ . |
| 10      | 73.0     | -288        | 3793    | 914     | Satisfactory test.                              |
| 11      | 0        |             |         |         | Inlet temperature too warm at start.            |
| 12      | 52.4     | -288        | 3793    | 846     | Satisfactory test.                              |
| 13      | 78.8     | -298        | 1397    | 554     | Satisfactory test.                              |
| 14      | 73.3     | -298        | 1897    | 491     | Satisfactory test.                              |
| 15      | 23.2     | -298        | 1897    |         | C/O by bad flowmeter coil.                      |
| 16      | 22.1     | -298        | 1897    |         | C/O by bad flowmeter coil.                      |
| 17      | 97.0     | -298        | 1897    | 385     | Satisfactory test.                              |
| 18      | 96.8     | -298        | 1897    | 385     | Satisfactory test.                              |
| 19      | 113.4    | -298        | 1897    | 307     | No cavitation. Insufficient screen $\Delta P$ . |

| Run No. | Duration | Temperature | N (rpm) | Q (gpm) | Remarks                                                      |
|---------|----------|-------------|---------|---------|--------------------------------------------------------------|
| 20      | 70.6     | -298        | 2529    | 597     | Satisfactory test.                                           |
| 21      | 58.7     | -298        | 2529    | 554     | Satisfactory test.                                           |
| 22      | 78.2     | -298        | 2529    | 461     | Satisfactory test.                                           |
| 23      | 65.4     | -298        | 2529    | 384     | Satisfactory test.                                           |
| 24      | 25.6     | -298        | 3161    | 589     | C/O by screen $\Delta P$ exceeding redline value of 35 psid. |
| 25      | 125.7    | -298        | 3161    | 589     | C/O due to flow oscillations.                                |
| 26      | 116.8    | -298        | 1897    | 527     | Satisfactory test.                                           |
| 27      | 83.0     | -298        | 1897    | 443     | Satisfactory test.                                           |
| 28      | 76.0     | -298        | 2529    | 579     | Satisfactory test.                                           |
| 29      | 59.8     | -298        | 2529    | 579     | Repeat of test 28 to check flow control.                     |
| 30      | 54.6     | -298        | 3161    | 589     | Flow control bad - no valid data.                            |
| 31      | 78.0     | -298        | 3161    | 589     | Satisfactory fixed throttle valve cavitation test.           |
| 32      | 0        |             |         |         | No start due to bad bearing temperature bulb.                |
| 33      | 56.0     | -298        | 3161    | 589     | Satisfactory fixed throttle valve test.                      |
| 34      | 77.2     | -298        | 3793    | 609     | Satisfactory fixed throttle valve test.                      |
| 35      | 72.3     | -298        | 2529    | 579     | Satisfactory test.                                           |
| 36      | 59.4     | -298        | 2529    | 517     | Satisfactory test.                                           |
| 37      | 89.2     | -298        | 2529    | 340     | Satisfactory test.                                           |
| 38      | 59.4     | -298        | 2529    | 650     | Satisfactory test.                                           |
|         | 57.0     | -298        | 2529    | 700     | C/O by screen $\Delta P$ exceeding redline value of 35 psid. |

| Run No. | Duration | Temperature | N (rpm) | Q (gpm) | Remarks                                      |
|---------|----------|-------------|---------|---------|----------------------------------------------|
| 40      | 66.1     | -298        | 3161    | 650     | Satisfactory test.                           |
| 41      | 53.3     | -298        | 1897    | 650     | Satisfactory test.                           |
| 42      | 246.1    | -298        | 1897    | 307     | Satisfactory test.                           |
| 43      | 69.3     | -298        | 3793    | 609     | Satisfactory test.                           |
| 44      | 194.9    | -298        | 2529    | 280     | Pump discharge pressure oscillated (surges). |
| 45      | 52.4     | -298        | 2529    | 700     | Satisfactory test.                           |
| 46      | 36.7     | -298        | 3793    | 1054    | Satisfactory test.                           |
| 47      | 23.4     | -276        | 2529    | 800     | Satisfactory test.                           |



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13. ABSTRACT

A mathematical model and a digital computer program were developed for the design and evaluation of inducers for pumping two-phase oxygen. The mathematical model indicated that any design would have to be run at relatively low speeds to pump two-phase oxygen and that the Rocketdyne J-2S oxygen inducer was close to optimum and should perform the task. A J-2S pump was built up and tested in two-phase oxygen at speeds indicated by the mathematical model. The pump was able to handle two-phase oxygen over a wide range of inducer incidence angles and up to a vapor content of 37 percent by volume with very little loss in head. The pump was still able to produce some of vapor fractions approaching 60 percent.

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| 14 | KEY WORDS                          | LINK A |    | LINK B |    | LINK C |    |
|----|------------------------------------|--------|----|--------|----|--------|----|
|    |                                    | ROLE   | WT | ROLE   | WT | ROLE   | WT |
|    | INDUCER TWO-PHASE FLOW ANALYSIS    |        |    |        |    |        |    |
|    | LITERATURE SEARCH                  |        |    |        |    |        |    |
|    | MATHEMATICAL MODEL                 |        |    |        |    |        |    |
|    | INDUCER DESIGN                     |        |    |        |    |        |    |
|    | INDUCER FABRICATION                |        |    |        |    |        |    |
|    | TWO-PHASE PERFORMANCE              |        |    |        |    |        |    |
|    | COMPARISON OF DATA AND PREDICTIONS |        |    |        |    |        |    |

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